The WELDING JOURNAL

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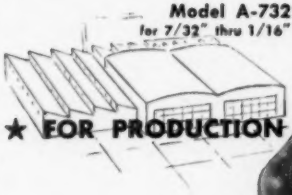

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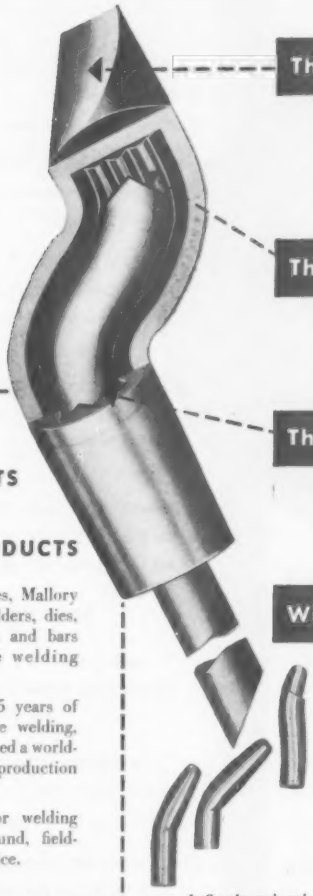

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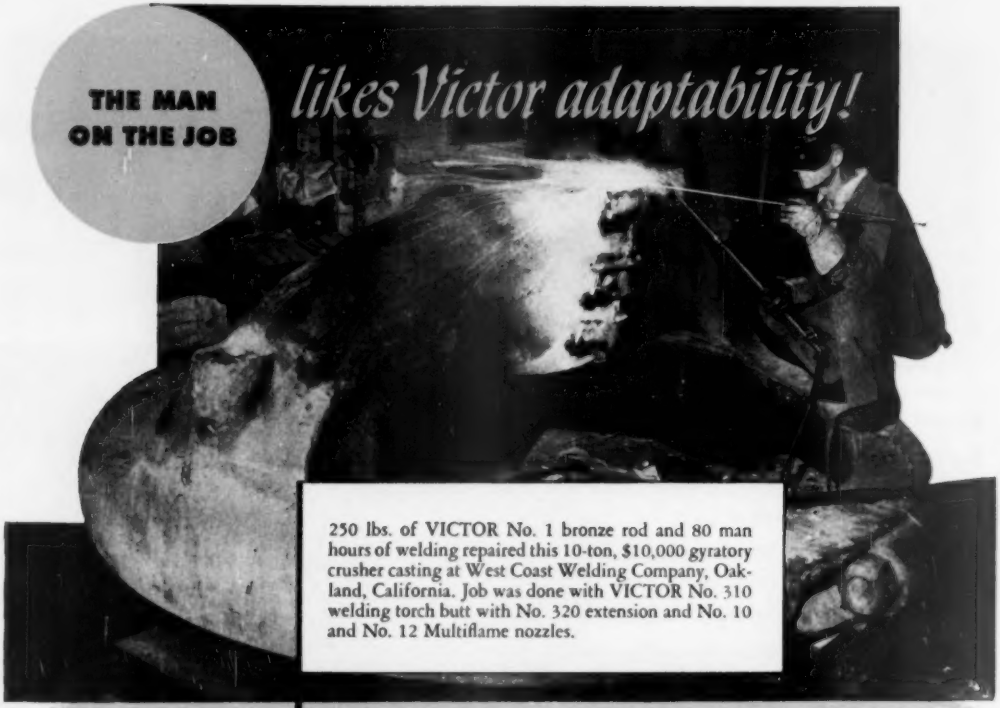

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The WELDING JOURNAL

volume 31 November 1952 number 11

Distortion Control in Structural Fabrication

Practical methods employed to control distortion in welded products by careful studies of the magnitude and direction of contraction

by Gordon Cape and Llewellyn Jehu

THE purpose of this paper is to illustrate the practical methods employed to control distortion of welded products in a large Canadian steel fabricating plant. The paper will review some of the fundamental aspects associated with welding, the difficulties which have arisen and the methods employed in the control of welded work.

The type of product fabricated in this plant is extremely varied. The structural output varies from a storefront to a 33,000-ton bridge. The Mechanical Shops produce a great variety of work ranging from a turned bolt to a 285-ton powerhouse crane. The Plate and Boiler Divisions turn out vessels varying from an underground storage tank to a 167-ton oil refinery fractionating tower shipped as a single unit, in addition to a wide range of heating and power boilers. Riveting is still the most important means of joining structural work in this plant but welding plays a very important part in a great number of the major structures. Plate and boiler work is now 100% welded.

The extremely wide variety of product requires the exercise of rigid control over all phases of construction of products which are unusual in design or fabrication. Control commences in the Design Department and continues through all operations to final inspection.

As in most companies, the welding personnel specialize in the various functions of design, detail, supervision and inspection, etc. These individuals are wholly engaged in direct production. As a result, they cannot spend much time in planning unusual work without neglecting their normal duties. To avoid this condition, a department staffed by engineers who have had shop, office and field experience has been organized. It acts in a consulting capacity to all other departments within the organization. Its responsibility consists of the study and planning of work of a special or unusual nature in order to produce fabricating procedure specifications which are used to guide design, detail and shop fabrication. These specifications are based on experience and the use of rules which have been developed over the years. The rules are accepted as fundamental principles which have resulted from both harrowing experiences and proven results in practical welding. They are relatively few in number and are not infallible in use due to the many factors which influence welding, but they do serve as a useful guide toward safe procedures. They are based on one fundamental fact that, "The local heating applied in welding always causes contraction."

In welding, the weld metal and a portion of adjacent material become heated to a temperature at which plastic flow occurs in the heated portion. Upon cooling, stresses are induced whose magnitude may equal or exceed the yield point of the material. These stresses create internal tensile forces which are balanced by corresponding compressive forces.

Although this condition is fundamental and is known by all who have had even an elementary knowledge of welding, it is surprising how often contraction has been neglected. The word "always" with reference to the occurrence of contraction cannot be overemphasized. Welding procedure controls start with the knowledge that contraction must occur and subsequently deal with its magnitude and the direction which it follows. It is desirable to know the magnitude of contraction but it is essential to know the direction of contraction.

The magnitude of contraction varies with a great number of factors but it can be evaluated with a sufficient degree of accuracy in most cases. The AMERICAN WELDING SOCIETY have published data which predicts

Gordon Cape and **Llewellyn Jehu** are connected with the Dominion Bridge Co., Ltd., Montreal, Quebec, Canada.

Presented at the Thirty-Third Annual Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Jan. 15, 1953.

approximate contraction allowances for individual welds.[1]

Contraction takes place in three directions—in the thickness of the weld, in its length and in its width.

In the thickness of the weld, contraction is relatively unrestrained and is compensated for by the addition of sufficient metal to provide proper reinforcement. In the length of the weld, contraction is restrained by the relatively large mass of adjacent material. The magnitude of contraction is small varying generally from $^1/_{32}$ to $^1/_8$ in. in 10 ft of length. In the transverse direction, restraint is generally less severe and as a result unit contraction is very much greater. The total amount of contraction per weld may be small but where a large number of transverse welds occur in a structure, the transverse shrinkage usually becomes more important than the longitudinal shrinkage.

The recognition of these facts rather than the exact magnitude of the shrinkage permits the preparation of procedures which will decrease stress and permit allowances where necessary to maintain the dimensional tolerances.

In addition, however, it must be recognized that contraction does not necessarily take place in a purely linear direction. If weld metal were deposited on the neutral axis of any weldment, pure linear contraction only would occur. For any other position with reference to the neutral axis, contraction will occur but the induced tensile forces eccentric to the neutral axis will, in addition, cause bending. The direction of bending leads to the first rule which is:

Rule No. 1. Contraction in welding will produce bending which is concave on that side of the neutral axis upon which the weld metal is placed.

A bead on the surface of a simple plate placed eccentrically with respect to the neutral axis will cause bending in the three principal directions—length, width, and thickness. Similarly welding on a complicated structure will cause bending, in any of the three principal directions, when the welding is not coincident with the neutral axes.

The amount of bending is more difficult to establish than the straight linear contraction. However, the knowledge of the direction of contraction and bending, combined with the other rules to follow, will reduce distortion to acceptable limits.

In the early days of welding in the company, a contract was undertaken to erect a bridge across the St. Lawrence River just outside of Montreal. The bridge consisted of a number of short deck spans and one main through span which were supported on the piers by welded pier members (Fig. 1).

The pier members for the short deck spans each consisted of a grid of vertical intersecting plates $1^1/_2$-in. thick welded to a base plate about 2-in. thick. Each unit was about 30-in. square by 18-in. high.

In planning the procedure of welding it was decided that bearing between the vertical ribs and base plate was of maximum importance. Consequently, the grid and base were assembled into a single unit and the grid-to-base welds were completed first to take advantage of the bearing produced by weld contraction. No difficulty arose until welding of the vertical joints between grid members was well advanced. Cracking then took place in each of the units under construction. After a considerable delay in trying variations in welding technique and peening, it was thought that preheating might help by reducing the differential contraction between weld and base material.

Preheating reduced the trouble to a very great extent but did not entirely eliminate it. It led, however, to the adoption of Rule No. 2 which may be stated as follows:

Rule No. 2. On thick or heavily restrained material, preheating will reduce cracking.

The effect of preheating is twofold. *One,* the expansion of the base metal due to preheating reduces the

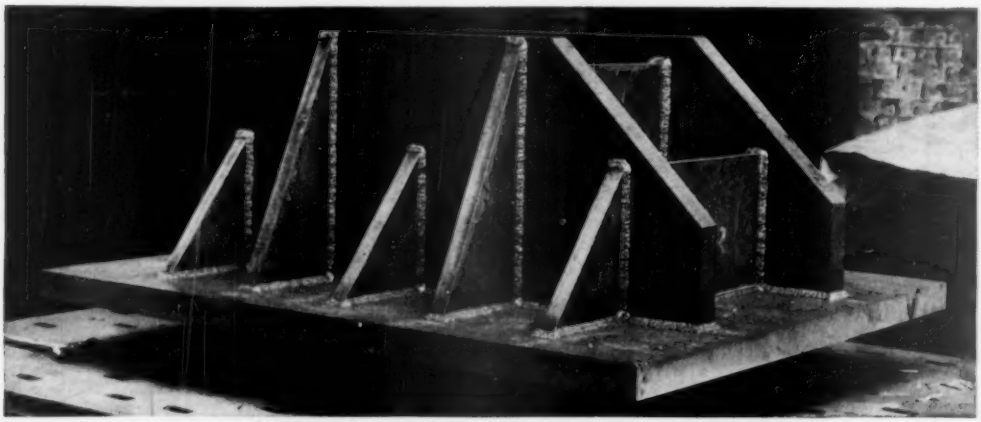

Fig. 1 Typical pier member showing single base plate and grid. Welded pier member, Abitibi Canyon Bridge

differential contraction between base metal and weld metal with a consequent reduction in residual stress. This was later proved by research undertaken at McGill University[2] in which residual stress was measured. Test plates maintained at various preheats during welding showed that residual stress decreased more than 30% at 300° F preheat compared to no preheat. *Two*, preheat reduces the rate of cooling with the result metallurgical changes are averted which could otherwise produce sufficient hardness to cause cracking.[3]

In welded fabrication the degree of contraction and metallurgical changes varies with the thickness of the material. There is an indistinct border between 1 and 1 1/2 in. which influences the procedure employed. Up to 1 in. in thickness, the chief concern in welded structures is the avoidance of distortion, whereas over 1 1/2 in. the main problem is the prevention of cracking. Preheating is used mainly to prevent cracking or reduce hardness and is consequently seldom used in structural steels less than 1 1/2 in. thick. The amount of preheat added to heated shop temperature is approximately 100° F per inch of thickness in excess of 1 1/2 in. but need not exceed 350° F. Normally this preheat can apply to a relatively narrow band adjacent to the weld and still be effective where restraint is not too severe and where it is more desirable to reduce hardness of weld and heat-affected zone.

Conditions do arise, however, where the temperature of preheat, when applied to narrow zones, is sufficient to reduce hardness but will not eliminate cracking. On one large dished baffle of a regenerator, where the dish was 19 ft in diameter and made in orange peel and dollar plate sections of 1 1/4-in. thick AISI Type 501 material, local preheats of 600° F were sufficient to reduce hardness to acceptable levels but insufficient to stop cracking. It was necessary to maintain this temperature over large areas on both sides of the joints in order to complete the welding without cracking. The value of full preheat may be judged from the fact that the first radial joints cracked when local preheat only was used, whereas the remaining radial joints, as well as the more highly restrained circular joint around the dollar plate, were welded without cracking when complete preheat was employed.

When the hardenability of the base metal due to heavy thickness or carbon content demands preheat and where restraint is not too severe, local preheating is often sufficient. Preheat may be applied in many ways, but is usually a costly and time-consuming operation. The effect of local preheat can be achieved through the procedure used in welding. A study of the S curve shows that weld and heat-affected metal cooled rapidly from welding temperature to below 400° F can result in a martensitic structure.[3] Sufficient preheat supplied in any manner will automatically delay the rate of cooling so that a change to pearlite is enforced. A first pass of weld metal on heavy material will normally cool in a short period of time at a rate which will promote the formation of martensite. If, however, the first pass of the weld is made intentionally short, quickly cleaned and is covered by a second and possibly third layer, the additional heat will slow up the rate of cooling of the first pass to avoid the formation of martensite. Furthermore, the second pass anneals the first to improve the grain structure and the heat applied in laying the first pass supplies preheat for the subsequent passes, again slowing up the cooling rate.

As an example, when high carbon nonstressed wear plates were welded to a supporting structure, the stitch welds were made in three passes as stated above, without any cracking and without any preheat other than the welding operation. Again, structures which involve too much loss of production time in torch preheating or which, if fully preheated, would be too uncomfortable for the welding operators, may often be successfully welded in this manner. As an example, welding the web system into a thick wrapper sheet to make a hoisting drum on a lift bridge (Fig. 2) can be performed without difficulty by completing the weld metal for full thickness in reasonably short increments of length to secure and maintain local preheat.

Fig. 2 All-welded hoist drum

Another means of supplying local preheat is obtained from the use of low hydrogen electrodes, which are less susceptible to cracking than the more conventional types. On the butt welding of heavy material, up to 5 or 6-in. thick, where transverse restraint is low, the first 1/4 to 1/2 of the depth of the joint is made with low hydrogen electrodes. A change to conventional types under the preheat conditions established can then be made to complete the joint.

After the successful completion of the small pier members of the St. Lawrence River bridge previously mentioned, larger pier members were started using the same procedure. The grid was welded to the base to ensure bearing before the grid members were welded together and preheat was used. The previous success of preheating gave some hope of completing the work without too much difficulty.

This thought, however, proved to be highly erroneous. After completion of the grid-to-base welds, the vertical welds between grid members could not be completed without cracking, regardless of the temperature of preheating. The first two were eventually finished

after repeated operations of chipping and stress relieving many times.

It was realized that a different welding procedure must be adopted. It was felt that if the grid had not been attached to the base plate, the vertical grid welds would be free to contract without overstressing to the point of failure. The next grid was then assembled into a complete unit free from the base with sufficient bracing to prevent lateral deformation of the grid. The unit was then welded with no difficulty whatever. The assembly of the grid to the base plate could not follow immediately since the lower surface of the grid was too uneven to provide the necessary bearing. A slight amount of machining, however, corrected the difficulty. When the grid was assembled to the base plate, excellent contact over the entire area was obtained and no trouble whatever arose in welding. A new rule thus came into being:

Rule No. 3. Subdivide weldments into separate restraint-free subassemblies where distortion effects can be corrected prior to or during final assembly.

This rule is followed more conscientiously than any other in planning original design and fabricating procedures. It is applied to any structure whether it be a pier member, paper mill slice, press platen, pressure vessel, crane bridge girder, end truck, trolley frame, etc.

As previously stated, contraction of weld metal in a joint occurs in three directions. Restraint in any direction results in stress. When the restraint is sufficiently severe, the weld metal may crack. A reduction of restraint can eliminate the cracking tendency. Restraint may be both internal and external to the joint. The internal restraint may be alleviated to a degree by such factors as preheating, peening, etc. The external restraint is usually capable of being controlled and it follows that any change of external restraint may then determine the tendency for cracking. This is where the value of subassemblies exists since a suitable choice of subassembly can usually be made to reduce external restraint.

It is then only necessary to visualize the direction of movement of components if free to contract during welding, contrasted to the distortion or cracking which could result if restrained by other portions of the same structure. This type of study reveals that all structures can be subdivided into several basic types—a total of four in number. For the purpose of distinguishing between each of these types and also to provide general guidance in the successful welding of each, a summary was prepared for insertion in the company's Welding Procedure Specifications and is included as follows:

"7. CONTROL OF DISTORTION & SHRINKAGE STRESSES:

Assembly & Welding Procedure

(*b*) The assembly and welding procedure shall be predicated by the type of structure to be welded as follows.

Type 1—Structures, having joints running in one direction only.

The structure may be completely assembled.

Each joint may be welded using a sequence which will avoid distortion such as—

(i) welding of the second side of the joint before completing the first side,
(ii) stepback sequence
(iii) wandering sequence,
(iv) cascade sequence,
(v) use of temporary strongbacks or holding lugs,
(vi) presetting to counteract distortion produced by welding,
(vii) peening.

All joints shall be made using a sequence which will balance the applied heat on various sides or joints to avoid bending of the structure as a unit by—

(i) welding joints simultaneously,
(ii) alternate welding of joints,
(iii) use of restraining fixtures,
(iv) peening.

Type 2—Structures, having joints running in two directions normally at right angles to one another.

Structures shall be assembled in a manner which will permit initial welding of any joints which would be restrained by prior welding (or heavy tacking) of other joints.

The welding of the joints shall be done as stated in Type 1 structures.

Type 3—Structures, having joints running in three directions with each joint normally at right angles to the other two.

Such structures shall be separated into Types 1 and 2 structural units, each of which shall be assembled and welded separately before welding together into the final structure.

Final welding of the structure shall generally start from the center, completing each weld in turn closest to the center, while balancing the applied heat on various sides to avoid undue distortion as stated in Types 1 and 2 structures.

Type 4—Structures, composed of divisible units of Types 1, 2 or 3 structures.

Such structures shall be separately assembled and welded as Types 1, 2 or 3 structures before assembly and welding into the final weldment.

Closure joints in Type 4 structures shall be welded in the following manner:

(i) Complete butt welds before making fillet welds.
(ii) Butt welds under complete or heavy restraint shall be prepared for welding from one side only, preheated to 212° F or higher and welded without interruption until the joint is completed."

Examples which may be used to illustrate the application of the assembly and welding procedure can be given by a consideration of simple structures. A can section of a pressure vessel having two or more longitudinal joints is a Type 1 structure. Two or more such cans joined together to make a complete shell become a Type 2 structure since longitudinal and girth joints are at right angles to one another. The method of welding suggested is to complete the longitudinal joints before welding the girth joints.

The grid of the pier member previously discussed is a

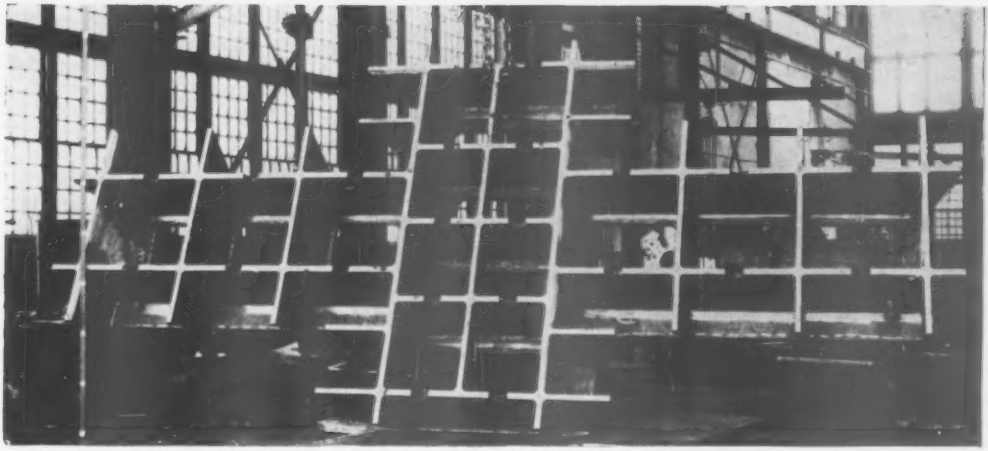

Fig. 3 (a) Typical cruciform grid subassembly

Type 1 structure. The grid and base plate assembly is a Type 2 structure.

A specific example of a Type 3 structure is a pier member for the Vancouver Lions' Gate Bridge (Fig. 3) which was made of a grid with top and bottom cover plates. The size of each of these members was 2 ft deep and 12 ft wide by 18 ft long. Plates varied from $1^1/_2$ to $2^1/_2$ in. thick.

The top and bottom cover plates were ordered as three separate plates due to the cruciform shape of the pier member. These plates constitute a Type 1 structure and were consequently welded as such. The grid, also cruciform in plan, was again a Type 1 structure. Due to the difficulty in predicting the exact over-all shrinkage, each of the three sections of the cruciform was welded separately so that allowance could be made in final assembly of the grid unit to compensate for errors in the estimated contraction allowance, including machining of top and bottom surfaces of the grid. The assembly of the grid and cover plates now became a Type 2 structure with welds running at right angles in two parallel planes. Here it becomes necessary to stop and consider the procedure which will permit a maximum of free contraction with a minimum of distortion and cracking. Two rules which may be here stated together were followed.

Rule No. 4. Weld from the center outward in both directions toward free ends.

Rule No. 5. Distribute the welding uniformly about the neutral axes.

The top and bottom flanges were assembled in position on the grid. Tack welds were applied only at the center of the grid. Contact between the grid and covers was obtained by bolts and yokes. Alignment was obtained by tack welding guide washers to the cover plates only on each side of the ends of the longitudinal grid members to permit unrestricted movement of the grid with respect to the covers.

Following Rule No. 4, welding progressed from the center and each cellular increment of length was completed for the full thickness before proceeding with the next. The function of the guide washers is evident from the fact that although welds applied parallel to the length of the grid cause equal longitudinal contraction of both grid and cover plates, welds at right angles to the length of the grid cause appreciable con-

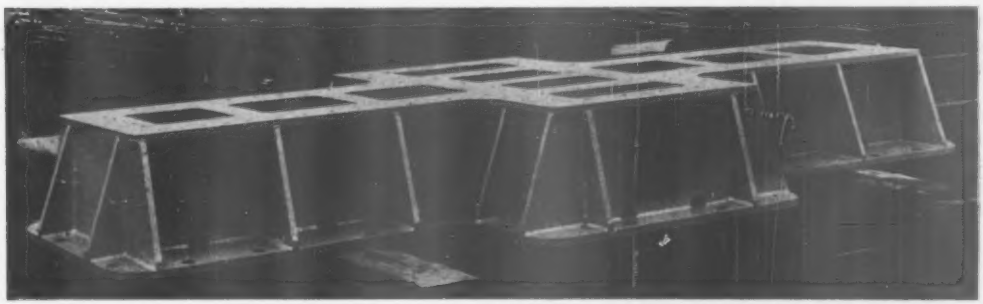

Fig. 3 (b) Typical complete pier member

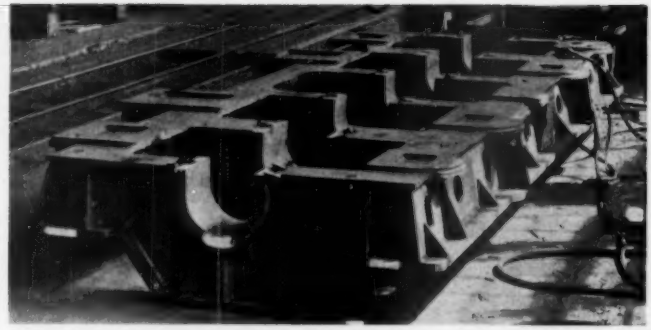

Fig. 4 Marine engine bedplate composed of eight "Type 3 structure" units

traction of the cover plates without any contraction of the grid system.

Following Rule No. 5, the welding of the first cover plate was completed only for the middle half length. Welding on the opposite cover plate was then completed for its full length. Finally the remainder of the first side was finished.

The lack of distortion is attested by the fact that the subsequent machining of top and bottom cover plates cleaned up at $^1/_8$-in. maximum machining depth.

Type 4 structures may be exemplified by a marine engine bed plate (Fig. 4) used to support a triple expansion engine and which in addition carries the bearings for the main crank shaft. These bed plates are essentially composed of two longitudinal and six transverse double-web box girders, each of which in itself constitutes a Type 3 structure. The assembly of a number of Type 3 structures combines to form the Type 4 structure.

Briefly, the control of distortion by use of subassemblies follows a simple rule of dividing a whole structure into Type 1, 2 and 3 subdivisions, again subdividing Type 3 subdivision into its basic components of Types 1 and 2. Thus all portions welded are relatively free from restraint and are capable of adjustment to compensate for unexpected distortion.

Subdivision of weldments in this manner has been used for many years in the production of welded bridges over 1000 ft in length, a press frame and a press platen each weighing over 60 tons, paper mill slices which are among the largest in existence, crane girders, grain car unloaders, etc.

The planning of welding procedures can be applied to the majority of structures following the rules described to permit freedom of movement for the elimination of cracking. In such cases movement is generally permitted to take place transverse to the direction of weld. There are conditions which arise in the welding of Type 2 structures which preclude movement in this direction making it essential to permit movement in one of the other two principal directions. Movement in any one of the three principal directions will provide a similar release of stress. This may be visualized by considering a rubber cube to represent an element of weld metal.

If the rubber cube were subjected to compressive forces on two opposite faces, the cube would bulge on the other four faces. If a second compressive force was applied to two opposite remaining faces, the cube would bulge still more on the two remaining faces. If next, compressive forces were applied simultaneously on all six faces of the cube, no bulging would occur even if the forces were extremely large.

A test was made in which a small rubber cylinder was inserted into a snug-fitting ring. An end force was applied to a piston which bore against the rubber until a load of 100,000 lb was reached without appreciable deformation of the rubber.

If now the rubber cube were subjected to equal tensile forces on each of its six faces, it can be reasonably assumed that deformation will not occur regardless of the magnitude of tensile forces, until failure occurs. If, however, the forces were released on two opposite faces, movement would occur in the other two directions of loading with a subsequent reduction of stress in all directions.

Weld metal in thick material is under high restraint in the direction of length, breadth and thickness after cooling. If movement is permitted in any one of these principal directions a reduction of stress will occur in the remaining two directions.

Certain types of Class 2 structures cannot permit movement in the width or length. Therefore, it be-

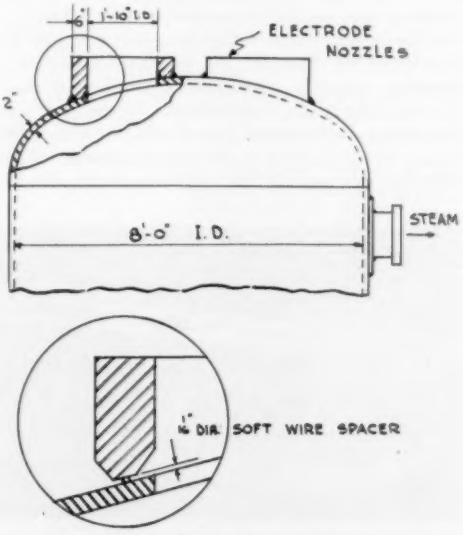

Fig. 5 Electric steam generator

comes essential to provide for movement in the direction of the height.

As an example, very heavy ring forgings welded to the surface of a dished head (Fig 5), are sufficiently rigid to prohibit movement in either the length or the width of the ring. Compressible spacers of small diameter soft wire, placed in the interface between forging and head, permit movement in the direction of the height of the ring. In this type of structure, compressible spacers eliminated cracking after all other methods failed.

There is still another case of a Class 2 structure in which provision for movement is impractical. A number of contract jobs have been done in which solid disks were butt welded into rigid plates. The size of disks has been as great as 12 in. diameter by 4 in. thick (Fig. 6).

The thickness of the material and the circular weld constitute three-dimensional restraint without means for movement to alleviate stress. These welds were successfully completed by observing three conditions:

(*a*) The joint design was made to permit complete welding from one side only of the work.

(*b*) The entire assembly was preheated to 350° F to decrease hardness at the root of the joint.

(*c*) Welding once commenced was carried on continuously until completed.

The reason for the elimination of cracking is due to the fact that as welding progresses, the temperature in the vicinity of the joint is higher than elsewhere. As a result, the disk tends to enlarge and the opening tends to decrease in diameter with the effect of imposing compression on the weld metal. Since compression cannot cause cracking, it follows that the maintenance of these conditions throughout the operation will overcome the possibility of cracking due to tension. Once the welding is completed and the work cool, the entire joint is under heavy residual tension but when complete for the full thickness of the joint it may be safely cooled down without fear of cracking.

Occasionally conditions may arise in which a Type 1 structure may impose very severe restraint on a joint. During the last war, the company produced a considerable number of 10,000-ton cargo vessels which were made by a combination of welding and riveting. The shell plating was riveted at the seams and to the frames but was welded at the butts (Fig. 7).

Due to the difficulty of obtaining sufficient welders, the riveting progressed at a much more rapid rate than the welding with the result that the butt joints were under heavy restraint before welding was started. This happened in the fall of the first season of operation. A considerable number of joints cracked. They were repaired and cracked again. In the meanwhile, a number of different welding techniques and sequences were applied but without success. The manner of failure was similar in every case. The cracks usually ran through the plating at some distance from the weld but never extended to the top and bottom edges of the plates. The fracture was zero width near top and bottom and open a maximum near the center. This was the clue which suggested a method of welding to overcome the trouble.

If two plate edges are butted tightly together and locally heated to a high temperature along the joint, the edges when hot will expand in length and bend convex so that the joint faces at mid-height will remain in contact while the faces at the ends will separate. Upon

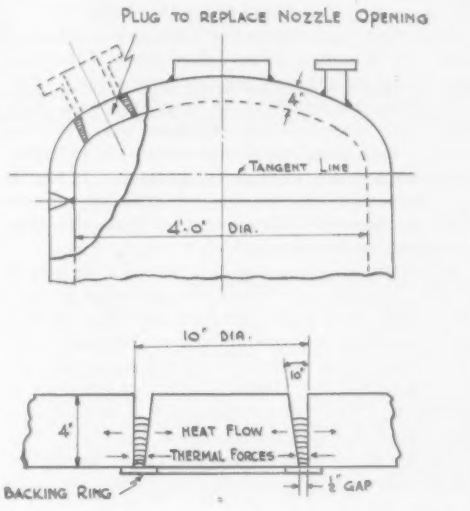

Fig. 6 Autoclave

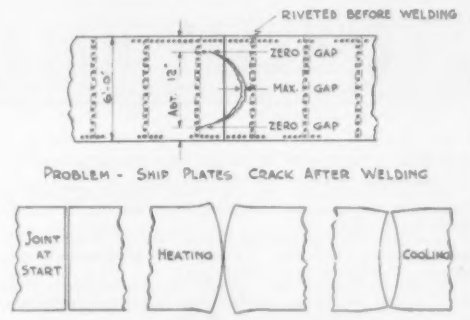

Figure 7

cooling, the edges of the plates will shorten to a length less than original, with the result that the faces at the center will separate while the faces at either end will close. The gap spacing between the plate edge is now similar to the gap spacing in the fracture. The gap enlargement at the center of the height suggests tension and the gap closure at the ends suggests compression across the width of the joint. Since the joint must have failed only in tension, the remedy suggested was to change the tension at the center into compression and tolerate tension of a reduced value at the ends.

This effect was obtained by making increments of weld 7 to 10 in. long for the full thickness of the joint and without stopping any increment once started. The first increment was made at the center—following increments were made consecutively above and below the center. Each increment in turn would tend to induce compression on the welds previously made. It was, of course imperative that the ends were at all times open to allow movement without contact until the weld was completed for the full length.

In the four following winters of operation, in spite of subzero weather and without the advantage of prior preheating, not a single butt joint cracked in production or in later service on approximately 50 ships.

The use of rules in planning welding procedures cannot be used blindly. There is usually an exception to every rule. This fact was borne out in the company shops where the wrapper sheet of a long boiler drum was rolled in one piece in contradiction to the drawing which showed three sections in the length of the wrapper sheet and tube sheet as well. The foreman of the shop decided that it would be ridiculous to cut the wrapper and then weld it once again into a single piece. The boiler drum was consequently assembled with three sections of tube sheet and one section of wrapper sheet.

Following the rules to which the shop is accustomed, the longitudinal joints were welded before the girth joints. The restraint now imposed on the girth joints was sufficient to produce cracking at each of the intersections. The drum also bent badly out of line due to the contraction of these welds.

In conclusion, it can be stated that control of distortion and stress in structural fabrication is concerned with two factors—internal restraint and external restraint. Internal restraint produces stresses which may be reduced during fabrication by such techniques as preheating, peening, welding continuously, cascading, etc. External restraint produces stresses which may be reduced or eliminated through the careful selection of subassemblies. The control of either or both in combination, will invariably provide the means of reducing distortion and cracking in weldments.

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(*Continued on page 1041*)

Quality Control for Spot Welding in a Job Shop

Quality control properly installed in a job shop pays for itself in better workmanship, lowered production costs and customer satisfaction

by Peter G. Poetto

THE purpose of this paper is to demonstrate the necessity for quality control in a welding shop.

Chapter 2, paragraph 1 of the WELDING HANDBOOK states that "Welding involves more sciences and variables than any other industrial process, which may explain why most of those concerned are satisfied with a very crude understanding of its problems."

The above seems particularly applicable to many of the job shops which employ resistance welding in the fabrication of assemblies for the armed forces. During the last three years, the truth of the above quoted paragraph has been manifested to the writer time and again. He has visited plants where resistance-welding equipment was being misused and abused. In a great many cases there were no quality standards to speak of. Yet many of these same machines had, at one time or another, been qualified in accordance with Military Specification 6860. However, once the qualifications tests were completed and the approval received, the personnel in charge either through ignorance or because of a desire to rush a production job would throw controls to the winds.

Needless to say, such a procedure always results in heavy rejections, costly reworks or worse yet the scrapping of parts with consequent production tie-ups, not to mention the increased cost of production.

It has been the ambition of this writer to demonstrate that quality control could be instituted in such shops, that it would eventually pay for itself, and that it would result in fine workmanship assist in the lowering of production costs and thus contribute materially to the defense program.

This ambition was realized last March when the writer was offered the position of Welding Engineer in charge of quality control at a plant located in New York City.

This paper is a descriptive report of the events which took place at this plant. It will present:

1. The conditions prior to the institution of a quality control program.
2. The steps taken to insure quality control.
3. The benefits derived from such a program.

The plant in question is one of the larger job welding plants in New York City. It is divided into two principal departments. Department No. 1 is the fabricating division and handles sheet metal layout, forming, stamping and deep drawing. Department No. 2 is the resistance-welding department, consisting, at the present writing, of 14 Sciaky stored energy resistance-welding machines which are used to join aluminum alloys, specifically 24S, 52s and 61s. In addition, it is equipped with anodizing and painting facilities.

Prior to the outbreak of hostilities in Korea, the plant was occupied with the resistance welding of such commercial items as aluminum containers and cabinets. Component sheet metal parts were fabricated and then passed on to the resistance-welding department for final assembly. The procedure in the welding shop was fairly simple. The foreman set up one of the Sciaky spot welders. He then provided himself with two pieces of material of the same gage and alloy as the production part being welded. He overlapped the two pieces without regard to the overlap or the edge distance in the production part and welded a single spot. The spot was then sheared by hand. If the test pieces sheared easily he would boost the heat and repeat the test until he was satisfied with the strength of the weld. The operator would then proceed to weld the assemblies until a point was reached where, because of excessive pickup and change in electrode diameters, it would become necessary to dress the electrodes.

Production would then stop and the electrodes would be dressed at the machine with the use of a rough file. While this method of arriving at a setting and maintaining a run might be satisfactory from a commercial viewpoint, the same procedure applied to work governed by rigid military specifications proved highly unsatisfactory.

When the assemblies were completely welded they would undergo a rather dubious type of final inspection.

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Assemblies which required painting were examined superficially. It was felt that whatever visual defects existed in the welded area would be covered up by a coating of paint.

Assemblies which were to be shipped without painting, were inspected with the aid of a hammer, a chisel and a wad of steel wool. If a spot showed evidence of copper pickup, the welded area would be polished with steel wool in an effort to eradicate the evidence. If metal expulsion had taken place between the sheets, the expelled material would be removed with the aid of a chisel. In the event that component parts of an assembly had not been assembled in accordance with blueprint specification, it was then an easy matter to break the spots since in a great many cases there was little or no fusion. The parts would then be reassembled and respotted. The fact that such a respotted assembly looked bad from an appearance standpoint did not seem to make too much difference.

An investigation of the inspection department setup and personnel revealed that the department was comprised of two inspectors. A checkup of the background of these men revealed that they had had no experience in the welding field and little or no knowledge of inspection procedures. In addition, it was discovered that both inspectors reported to, and were under the direct supervision of, the welding foreman. In reality the phrase "inspection department" was a misnomer; it might better have been labeled the repair department.

An investigation of the production department revealed that the welding operators had only a very crude understanding of the machines they were operating. To illustrate, the machines were equipped with the following controls:

(*a*) Precompression timer.
(*b*) Recompression timer.
(*c*) Heat control, amp.
(*d*) Air pressure gage.
(*e*) Contact gage.
(*f*) Switch for one and two cylinder pressures.
(*g*) Switch for variable and constant pressure.
(*h*) Repeat switch.

The only controls used were the pressure and heat dials. Precompression and recompression had been set at 40 cycles. Irrespective of the alloy, gage, overlap, edge distance and the electrode setup, the machines were run constantly at these values. Some machines were run on one cylinder, others were run on two cylinders: some with variable pressure, others with constant pressure. No attempt was made to correlate the controls with a specific setup, alloy, gage of material, overlap and edge distance. Calibration of the pressure gage in terms of electrode pressure had never been attempted. The relationships between welding pressures and forging pressures had never been worked out.

The correct practice of setting up electrodes had never been adhered to. Little attention was given to the inlet water cooling tubes. These tubes were found to be either in a damaged condition or missing. The use of properly designed electrodes for a specific run was not looked upon as an important factor and whenever a job presented itself which required specially designed electrodes, the electrodes were usually shaped at the machine with a file. As a result, the electrodes would stick to the workpieces causing severe pickup. No attempt had been made to classify electrodes according to class, shapes, tip diameter and radius.

Investigating the cleaning process prior to welding disclosed that the cleaning was carried on in three 100-gal tanks. The first tank was an alkaline cleaner. The second tank was a cold water rinse, while the third tank was an acid tank for deoxidizing purposes. No steps had been taken to insure proper drying procedures. The rinse tank was used both for rinsing the material from the alkaline tanks and the parts from the acid tank. Titration and pH control were unknown. The alkaline tank, when first examined by the writer, revealed a heavy sludge condition both on the bottom and the sides of the tank. No temperature controls were in evidence and no steps had been taken to insure the proper heating of the alkaline tank.

It is needless to say that such a disorganized procedure, from the cleaning stage to the welding stage and to the final inspection stage, began to take its toll in heavy rejections, rework and scrapped material.

Table 1 lists the number of rejections for a period of 6 months prior to the adoption of the Quality Control Program.

Table 1—Rejections Over Six-Month Period

All assemblies in this table were made of Aluminum 52S¹/₄ Hard

Job No.	*Gage comb.*	*No. of spots*	*Total assemblies*	*Total rejections**
A-107	0.032–0.032	32	8,500	2672
A-208	0.064–0.064	56	10,000	3786
A-215	0.040–0.064	60	5,000	1500
A-217	0.050–0.050	48	7,000	1653
A-219	0.032–0.080	28	8,000	3178
A-221	0.050–0.064	40	10,000	4675
A-222	0.064–0.090	16	5,000	1791

* Rejections were made by the inspection department of the Prime Contractors and the defects noted were as follows:

1. Poor fusion.
2. Heavy indentation.
3. Excessive spitting.
4. Cracks in center of weld.

From the information just cited, it was clear that the resistance-welding process, the cleaning process and the inspection methods were definitely out of control, and that a continuation of such practices would prove disasterous from a financial point of view.

This led to the setting up of a quality-control program.

INSPECTION DEPARTMENT

Three new inspectors were added to the inspection Department, each one having a knowledge of resistance-

welding inspection and the ability to read blueprints. Final inspection of weldments was abolished and in its place was substituted first piece inspection, followed by inspection of the assemblies as they came off the welding machines. This became known as floor inspection.

The inspection department was charged with the dimensional inspection of the first piece, and with visual inspection of all spots in the assemblies.

This inspection included a measurement of the indentation of all spots using a dial micrometer. All visual inspection was governed by the quality standards set forth in the following section of Military Specification 6860:

"*Appearance:* The outer surface of all spot welds shall be smooth, free of cracks, essentially free of cracks and pits which indicate that the spot welds were made with a dirty electrode or an improperly prepared surface, or that undue penetration of the weld nugget or diffusion of copper has taken place.

"*External Defects:* The following defects are not acceptable: Pits, surface flashes, tip pickup, expulsion of metal between the sheets, external cracks, edge bulge cracks and blown spots. Sheet separation between welds is not acceptable if it is in excess of 10% of the thickness of the thinnest sheet joined. Electrode indentation is not acceptable if it is in excess of 10% in depth of the thickness of the sheet in which the indentation occurs."

The reorganized inspection department was divorced completely from the production department, and was placed under the direct supervision of the welding engineer who supervised the quality control program.

TESTING LABORATORY

At the same time, a testing laboratory was set up and provided with the following equipment.

1. Tensile testing equipment.
2. Do-all band saw for cross-sectioning test specimen.
3. Grinding and polishing disks for preparing cross-sectioned specimens for macro etching.
4. Etching reagents for etching polished specimen.
5. Brinell microscope equipped with vernier for determining depth of penetration, condition of nugget.
6. Titration equipment for daily titration of cleaning solutions.
7. Microhmmeter with laboratory press used to check efficiency of cleaning process by checking contact resistivity on cleaned specimens.
8. In addition, a small radiographic unit will be installed shortly for radiographic inspection of aluminum rocket assemblies.

The testing laboratory, like the inspection department, was placed under the direct supervision of the Welding Engineer.

MACHINE MAINTENANCE DEPARTMENT

In order to assure an adequate system of controls, it became necessary to bring the machines in line and to keep them in good operating condition. Hence, there was organized a maintenance department headed by a man with years of experience in the mechanics of a popular make of stored-energy spot welders. Every machine was thoroughly checked and tested with newly purchased testing equipment.

The pressure gage was calibrated both for welding and forging pressure using a standard electrode pressure gage. Temperature and the rate of flow of the water were checked. All contactors were checked together with tubes and switches. Worn parts were discarded and replaced with new parts. A stock room was set up with a store of spare parts. An order was issued forbidding anyone but the maintenance mechanic to attempt repairs on the resistance-welding machines. Instructions were issued to the effect that, upon completion of repairs, the foreman in charge was expected to submit to the laboratory 25 test specimens to be checked for shear strength and macro etched to determine the consistency of the machine.

To prevent major breakdowns, a check list was drawn up and is now being used as a medium of preventative maintenance. Every machine is checked periodically.

Table 2—Typical Check List

To be referred to nightly by the maintenance mechanic. Items checked off and returned to Laboratory with date and signature of mechanic

Oper. No.	*Time*	*Operation*	*Description*
1	Every night	Check operation of contactors	Adjust rheostats by turning all eight completely counterclockwise, then close Nos. 3 and 6 approximately 10 deg clockwise, Nos. 2 and 7 approximately 20 deg and Nos. 1 and 8 approximately 30 deg. Nos. 4 and 5 are to remain completely counterclockwise. Operate machine and check distribution of arc between contactors.
2	Every night	Adjust neat's-foot oil supply	Neat's-foot oil lubricator should be adjusted so that there is always a very small amount of oil being fed into the air line.
3	Every 2 weeks	Calibrate pressure gage	Use electrode pressure gage.
15	Every 2 weeks	Grease all machines	Lubricate in the slot directly above ram guide.
22	Every 6 weeks	Check all electrical contacts	All electrical contacts exposed to dirt, grease, etc., are to be removed, cleaned and then replaced and finally tightened.
24	Every 6 weeks	Clean out water lines	The water system should be flushed by reversing the flow of water in the system and then blowing with high-pressure air.

Some items are checked more frequently than others. For example, contactors are checked nightly. On the other hand, the pressure is calibrated once every two weeks, and the secondary circuit is checked every six weeks.

A part reproduction of a check list is given in Table 2.

The control and maintenance of electrodes was assigned to the tool crib. Since the employee in charge of the tool crib was experienced in the use of a lathe, he was given the responsibility of dressing all electrodes. Each electrode was classified in accordance with alloy, tip diameter, radius and length. Each welding job card called for a specific set of electrodes. At the present time, the operator signs for a set of electrodes. When the electrodes show signs of excessive wear, the operator removes them from the machine, returns them to the tool crib and receives a newly machined set. The tool crib attendant then restores the radius by machining and stores the electrodes away for further use. In addition the tool crib has charge of welding fixtures, electrode arms, emery cloth and all other equipment necessary for the proper operation of the spot-welding machines. The tool crib is also under the direct supervision of the Welding Engineer.

CLEANING DEPARTMENT

Satisfactory weld strength and consistency of results, particularly in aluminum spot welding, depends, among other things, on the proper sheet surface preparation prior to welding.

A change in our procedure of cleaning was therefore necessary in order to achieve (1) welds of optimum weld strength, (2) optimum weld consistency, (3) uniform weld nugget symmetry, (4) minimized expulsion at interface or between tips, (5) improved surface appearance, (6) decreased electrode fouling and (7) minimizing of cracks in welds.

Five new tanks were installed each with a 500-gal capacity. Table 3 shows a diagram of the new sequence of operation in the cleaning of aluminum assemblies.

Table 3—Cleaning Process—Preparation of Aluminum for Spot Welding

Cleaning Tank No. 1

Commercial Cleaning Solution
Concentration: 5 oz per gal of water.
Operating Temp.: 160–170° F.
Time of Immersion: 1 to 3 min.

Rinse Tank No. 2

Cold running water. Tank equipped with overflow and drain. Water jets around top side of tank for final pressure spray as work leaves tank.

Deoxidizing Tank No. 3

Commercial Deoxidizing Solution
Concentration: 10 oz per gal of water.
Operating Temp: 60 to 70° F.

Tank No. 4

Cold rinse tank, equipped with overflow and drain.
Water jets for final spray as work leaves tank.

Tank No. 5

Boiling water, 212° F, to expedite drying.

The tanks are set in a row and equally spaced 48 in. apart. They are fed by an overhead hoist, electrically operated. Stainless steel baskets are suspended from the hoist. These are fabricated of Type 321 stainless steel alloy.

Tanks Nos. 1, 3 and 5 are equipped with thermostatic controls for proper control of temperature range. Each tank has an indicating temperature recording device.

Tank No. 1 contains a commercially available alkaline cleaning solution used for the removal of oil, stencil inks and forming compounds. The solution is run for a period of two weeks, eight hours daily, six days a week. It is dumped every two weeks, the tank is thoroughly cleaned of any accumulation of sludge and filled with a new solution. The solution is titrated daily. pH readings are taken every two days.

Following the cleaning cycle, the work is carried over to Tank No. 2, the rinse tank. The water in this tank is continually flowing to avoid contamination. The tank is also equipped with a perforated copper tubing running the length of the tank. As the basket is lifted from the rinse tank, it receives a final rinse from these jets.

Tank No. 3 is the deoxidizing solution (acid solution). This is a completely welded stainless-steel (Type 321) tank. The solution in this tank serves to remove the oxide coating, thereby controlling one of the important variables in resistance welding, namely, the contact resistivity. Immersion time in this tank is from five to seven minutes.

The deoxidizing process is followed by a cold rinse in Tank No. 4 which is similar to Tank No. 2. The work is then transferred to Tank No. 5. The hot water rinse will insure faster drying when the work is transferred to the work table.

The control of the cleaning solutions is under the direct supervision of the Welding Engineer and consists of the following:

1. Daily titration of the cleaning and deoxidizing solutions to determine the concentration and upkeep to be added.
2. Daily titration of cleaning solution to determine aluminum content.
3. pH readings every other day to determine alkalinity and acidity of baths.
4. Check of contact resistivity. This control has gone a long way toward eliminating our welding problems and deserves some mention here. Since there are no known visual means of determining the presence or absence of an oxide film on a sheet of aluminum, our only recourse was the use of a hydraulic press equipped with a pressure gage reading to 500 lb pressure. The hydraulic press is set up with a pair of dummy electrodes. The top electrode is a 4-in. radius electrode while the bottom has a radius of 10 in. To these electrodes are wired the leads from a microhmmeter which is capable of measuring electrical resistances down to 1 microhm. These test panels are of the same alloy and thickness as the assemblies. Upon completion of the cleaning process they are delivered to the test laboratory

where readings are taken and results noted in a log book. We like to keep our readings from 10 to 25 microhms.

There are certain conditions which must be standardized before running this test. They are as follows:

1. Tip radius: 4 and 10 in.
2. Electrode force: 500 lb.
3. Calibrating current: 15 amp.

The calibrating current is furnished by storage batteries and is measured by an ammeter.

CONTROLLED OPERATION

To insure quality production, control has to be exercised at the welding machines.

To begin with, a hard and fast regulation has been instituted. The responsibility for the setting up of a machine rests with the foreman. The operator will commence production only after a machine has been properly set up and the results approved by the laboratory.

This procedure takes the following form:

At the beginning of each work day, the foreman refers to the blueprint of each particular assembly to be welded. He then selects the proper electrodes as called for in the worksheet. Both operator and foreman then set up the machine. The foreman receives from the laboratory a specified number of test specimens of the same gage and alloy as the parts to be welded. He then refers to the setting card or welding schedule card for that particular assembly. The welding schedule card is located in a metal container attached to the side of the machine. This card contains the last acceptable setting used for this assembly. Using the same overlap and edge distance as called for on the assembly, the operator is then instructed by the foreman to weld nine test specimens, each specimen being a single-spot specimen, each part of the specimen measuring 4 in. in length and 1 in. in width. The test specimen together with the welding schedule card is then turned over to the laboratory.

The laboratory then proceeds to check all nine pieces for proper indentation, overlap, gage of material and edge distance. Three pieces are then tested in shear. The shear test must meet the minimum requirements of Military Specification 6860. Three additional pieces are cross sectioned, etched and examined with a 25-power Brinell microscope.

The standards used in the laboratory for acceptable macrostructures are those outlined in Specification 6860. Briefly, they are as follows:

1. Weld penetration and soundness shall be determined by examination of the etched section through the center of the spot weld. The outline of the fusion zone shall be substantially symmetrical and shall approximate an ellipse.

2. Internal defects: Weld defects such as porosity, cracks and lack of fusion, are acceptable if the maximum extent of the defect does not exceed one of the following limits:

15% of the weld diameter.
25% of extension into the outer sheet.
Extension within 15% of the weld diameter of the boundaries of the cast structure.
Penetration of the fused zone into the outer sheets shall not be less than 20% of the original sheet thickness and not over 80% of the sheet thickness.

Upon completion of the test, the laboratory technician fills out "The resistance-welding test data card." This card, together with the welding schedule card, is returned to the operator. The machine is now ready for production. The test pieces are placed in an appropriate envelope, the face of the envelope is used to record the machine number, operator number, date, time, alloy gage combination, part number and the amount of penetration. The envelope is then filed for future reference.

This test is repeated every 2 hr or whenever electrodes are changed. Failing to pass a test results in the shutting down of the machine. The machine then is given a complete check. New tests are then resubmitted to the laboratory for approval.

TRAINING PROGRAM

This is one part of the quality control setup which has not been touched upon in this paper but which the writer feels should be presented briefly because of its importance in implementing the entire picture.

It was deemed necessary, at the start of our new program, to orientate our operators to the field of aircraft welding. Some sort of motivation was necessary to arouse interest in the work and a sense of pride in what they were doing. Two hours each week were therefore set aside for a weekly meeting conducted by the Welding Engineer.

A sound projector was purchased and technical films on correct procedure for welding were borrowed. Each week the chairman of the meeting spoke on some phase of resistance welding which had bearing on their immediate work. This was followed by an open discussion period and operators were asked to present their problems. The discussion period was followed by the showing of a film. One operator each week was assigned to the laboratory as a helper. In this way he participated in the complete testing and examination of

Table 4—Spot-Welding Production for Month of October 1951

Job No.	*Assemblies welded*	*Cost*	*Assemblies rejected*	*Cost for rework, dollars*
A-107	8,500	$ 8,160	2672	$ 2,574.12
A-1021	2,000	2,240	625	926.00
A-1022	2,500	1,050	475	227.00
A-1026	1,500	2,100	950	798.00
A-1028	1,000	1,500	95	175.00
A-1030	3,000	4,050	800	1,126.00
A-1035	8,000	5,240	1070	834.60
A-1038	10,000	16,800	2543	4,272.24
Total	36,500	$42,140	9230	$10,932.96

Table 5—Spot-Welding Production for the Month of June 1952

Job No.	Assemblies welded	Cost	Assemblies rejected	Cost for rework, dollars
1461	3,850	4,851.00	86	$108.36
1462	6,500	4,800.00	15	9.00
1463	10,000	8,400.00	100	84.00
1464	8,500	6,120.00	0	000.00
1465	1,050	1,764.00	0	000.00
1466	3,000	3,600.00	22	9.46
1467	8,000	7,680.00	35	33.60
1468	7,500	13,500.00	14	25.20
1469	8,050	3,622.50	11	4.95
1470	10,000	7,200.00	3	2.16
1471	5,000	7,500.00	0	000.00
1472	4,950	3,564.00	7	5.04
Total	76,400	$72,601.50	293	$281.77

the welds. The operator then presented his experience in the laboratory to the group at the weekly meeting.

This program has aroused in the operators a sense of pride and accomplishment. This spirit is reflected in the effort they make to produce a better welding job. This has resulted in a contest among the operators, each trying to hold down his rejections to a minimum.

ECONOMICS OF QUALITY CONTROL IN RESISTANCE WELDING

Tables 4 and 5 are a tabulation of the amount of spot welding performed during a months' period. Two typical months are presented.

Table 4 shows the amount of assemblies welded for a particular job, the costs for the spot-welding operation.

This cost includes; cleaning, jigging, spot-welding time and packaging. It indicates the amount of rejections for each particular lot, the rejections being the amount of assemblies sent back by the Prime contractor for rework and the cost for said rework. This is for the month of October 1951, 5 months prior to the introduction of the quality-control program.

Table 5 is similar to Table 4. It presents figures for the month of June, 3 months after instituting the quality-control program.

CONCLUSION

At the outset of our program, we were warned on several occasions that we were going overboard on this business of quality control. We were told that the expense involved would eat into our profits; that it was time consuming and would bottleneck our entire production. The proof of the pudding, however, is in the eating.

These are the results after 4 months of operation under our new setup:

1. The quality of our welding has improved 100 %. Our rejections have been cut to a minimum. This, in itself, has paid for all the expenses of the program. Rework is a costly operation, being bad both financially and for the reputation of the firm.

2. The attitude of our employees has changed for the better. They take pride in their work and have developed a keen sense of carefulness and responsibility toward their work.

3. While production decreased somewhat at the beginning, we have now more than gained the lost ground. The output per man has increased materially.

4. Two weeks ago, we inaugurated a third shift because of the increasing work load. Within the past month we have obtained three new contracts which has necessitated a third shift and the purchase of two seam welders, three-phase synchronous controls and one spot welder capable of welding aluminum alloys $^1/_4$ in. in thickness. This certainly is not a sign of a decrease in productivity.

5. Last, but not least, we feel that we are rendering a real service to our Armed Forces. The assemblies that leave here have the stamp of approval, approval gained through proper engineering and testing. This, in itself, is a great moral satisfaction.

Come — To the welding-minded Southwest where America has invested billions in creating a new industrial empire

See — The Welding Industry's newest products designed to cut cost and speed production

Tour — The tremendous industrial development along Houston's Ship Channel; now second in U. S. Shipping tonnage.

Joint Design for Brazing

Selection of base and filler metals, types of joints, requirements as to strength, conductivity and tightness for various services

by W. J. Van Natten

THE design of a joint to be brazed is dependent on a number of factors. The most important and best controlled are as follows:

I. Selection of Base Metals and Filler Metal.
II. Types of Joints.
III. Service Requirements.
  A. Mechanical Strength.
  B. Electrical Conductivity.
  C. Pressure Tightness.
  D. Elevated and Subzero Service Temperatures.

The following sections on joint design are of fundamental nature and will be applicable for use with most of the generally used base materials and brazing filler metals.

I. SELECTION OF BASE METALS AND FILLER METAL

To begin with, it is assumed that the designer has basic knowledge of the part or assembly to be designed and its functions. From this information the base metal that will best suit the application is determined.

There are usually one or more applicable filler metals for the generally used base metals. Table 1 shows base metals indexed against each other to give the recommended filler metals.

A note of caution is in order at this point. Where base metals having different coefficients of thermal expansion are to be brazed care must be taken that a stress that can cause failure of the base metal or joint is not introduced during or after brazing.

Table 1—Base Material Vs. Brazing Filler Metals (Similar and Dissimilar Combinations)

Base Material	Al and Al Alloys	Mg and Mg Alloys	Ni and Ni Alloys	Carbon & Low Alloy Steels	Stainless Steels	Iron	Copper & Copper Alloys	High Carbon & Tool Steels	Heat Resistant Alloys
Al & Al Alloys	BAlSi								
Mg & Mg Alloys	X	BMg							
Ni & Ni Alloys	X	X	BAg,BCu BCuZn BNiCr						
Carbon & Low Alloy Steels	X	X	BAg,BCu BCuZn BNiCr	BAg,BCu BCuZn BNiCr					
Stainless Steels	X	X	BAg,BCu BCuZn BNiCr	BAg,BCu BCuZn BNiCr	BAg,BCu BCuZn BNiCr				
Irons	X	X	BAg,BCu BCuZn	BAg,BCu BCuZn	BAg,BCu BCuZn	BAg,BCu BCuZn			
Copper & Copper Alloys	X	X	BAg, BCuZn	BAg, BCuZn	BAg, BCuZn	BAg, BCuZn	BAg,BCuP BCuZn		
High Carbon & Tool Steels	X	X	BAg,BCu BCuZn	BAg,BCu BCuZn	BAg,BCu BCuZn	BAg,BCu BCuZn	BAg BCuZn	BAg,BCu BCuZn	
Heat Resistant Alloys	X	X	BAg,BCu BCuZn BNiCr	BAg,BCu BCuZn BNiCr	BAg,BCu BCuZn BAgMn BNiCr	BAg,BCu BCuZn BNiCr	BAg BCuZn	BAg,BCu BCuZn	BAg,BCu BCuZn BAgMn BNiCr

X = NOT RECOMMENDED

NOTE: BRAZING FILLER METALS ARE DESIGNATED BY AWS - ASTM CLASSIFICATION (SEE THE WELDING JOURNAL AUGUST 1952)

II. TYPES OF JOINTS

There are two, and only two, basic types of joint design used for brazing. These two are the lap joint illustrated in Fig. 1 and the butt joint illustrated in Fig. 2. There are, however, many variations of the lap joint, variations of the butt joint and combinations of lap and butt joint used in brazing applications. Some of these are illustrated in Figs. 5 through 18.

The selection of a joint design for a specific application will depend largely upon the service requirements of the assembly but may also be dependent on such factors as the brazing process to be employed, fabrication techniques prior to brazing, quantities to be brazed, method of applying filler metal, and others.

When the service requirements of a joint are severe it generally means that a leak-tight joint approaching 100% efficiency is desired. In other words, the load-carrying capacity of the joint should approach or exceed the load-carrying capacity of the weakest member of the assembly.

The mechanical properties of a brazing filler metal in a joint seldom match the mechanical properties of the base metals being joined. The unit strength of the filler metal occasionally is higher than that of the base metal but more generally it is considerably lower. The unit strength of the filler metal is also affected by the joint clearance, as will be explained later, and by the presence of defects such as porosity, flux inclusions and unbrazed areas.

In the lap joint (Fig. 1) the area of overlap may be adjusted so that the joint will be as strong as the weakest member despite the lower unit strength in the filler metal or the presence of small defects in the braze. An overlap greater than three times the

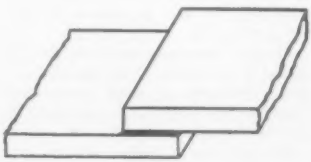

Figure 1

Figure 2

W. J. Van Natten is connected with the Schenectady Works Laboratory of the General Electric Co., Schenectady, N. Y.

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thickness of the thinnest member will usually yield 100% joint efficiency. The greater the overlap the greater will be the factor of safety.

Lap joints should be employed wherever possible, since the possibility of obtaining leak-tight joints with 100% efficiency is greatest. They do have the disadvantage of increasing the metal thickness.

Butt joints (Fig. 2) are limited in joint area to the cross-sectional area of one member. If a high joint efficiency is to be obtained, a filler metal whose unit strength matches that of the base metal must be employed and the braze must be free of defects. It is difficult to fulfill these requirements in most applications; therefore butt joints should only be used where service requirements are not severe and where leak tightness and strength are relatively unimportant.

Tee joints and corner joints are considered to be butt joints. Scarf joints (Fig. 3), a variation of butt joints, may be employed to increase the joint area. Their use is somewhat questionable unless the joint area is over three times the normal cross-sectional area. Scarf joints are more difficult to prepare and harder to hold in alignment during brazing than are lap or butt joints.

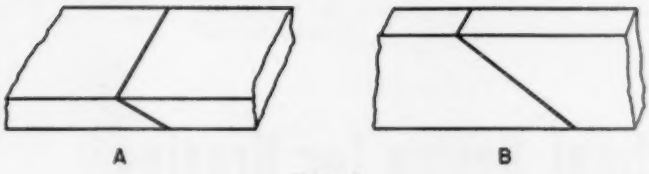
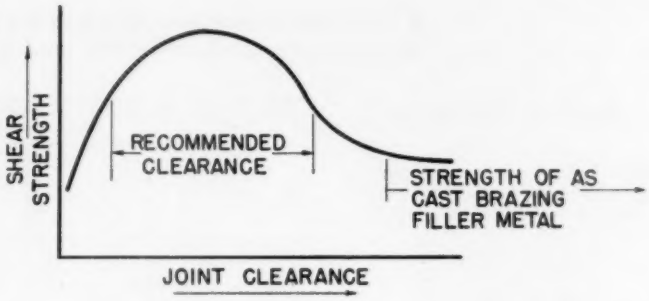


Figure 3



SHEAR STRENGTH VS. JOINT CLEARANCE
(BRAZING LOW CARBON STEEL WITH SILVER ALLOY FILLER METAL)

Figure 4

III. SERVICE REQUIREMENTS

A. Mechanical Strength

A joint may be subjected to various types of stresses such as tension, shear, impact and fatigue. The effect of different factors on the mechanical strength of brazed joints is discussed under this section in the following pages.

1. Joint Clearance Vs. Mechanical Strength. Joint clearance has a direct bearing on the mechancial strength of any brazed joint no matter how it is stressed.

It has been proved that joint clearance with its effect on the subsequent thickness of brazing filler metal in the joint is a factor in obtaining the maximum strength for a given design. In this discussion "clearance" is the dimension between the interfaces of the completed brazed joint.

However, clearance between the brazed parts must be considered in terms of conditions at one specific instant—i.e., room temperature or brazing temperature. With similar metals, of about equal mass, the room temperature (before brazing) clearance is a satisfactory guide. For dissimilar metals, those with higher thermal expansion may tend to close up or widen clearance, depending upon the disposition of the metals. Thus when brazing dissimilar metals (or greatly differing masses of similar metals) consideration must be given to the clearance at brazing temperature and adjustments made in the room temperature clearance to achieve the desired clearance at brazing temperature.

Since measurement of clearance at brazing temperature is virtually impossible the best practical measure of this value is the clearance after brazing. This may be difficult to measure with those brazing filler metals which exhibit diffusion. Therefore, for practical production considerations, the room temperature before brazing is the controlling condition for joint clearance.

As an illustration of the influence in joint clearance (as measured on completed brazed joints) consider a brazed joint of carbon steel to carbon steel as shown in Fig. 4. This represents joints made with BAg-1 filler metal and flux and plots shear strength vs. joint clearance.

The curve would not always be as pronounced as shown in Fig. 4. However, it has been generally accepted that for each particular filler metal there is a best joint clearance range which will give the best joint strength.

Table 2 may be used as a guide in designing for maximum joint strength. If clearances are used which are less than those recommended, the joint strength may fall appreciably due to excessive voids, flux inclusions, etc. Larger clearances will permit greater flexibility in machining but may be wasteful of filler metal and parts.

The joint clearances shown in Table 2 should be maintained at brazing temperature; clearances at room temperature should include allowance for expansion at the higher temperature of brazing.

When brazing dissimilar materials it is advisable to check the coefficients of thermal expansion especially in the brazing temperature range, since a number of difficulties may arise. Two of these are described as follows:

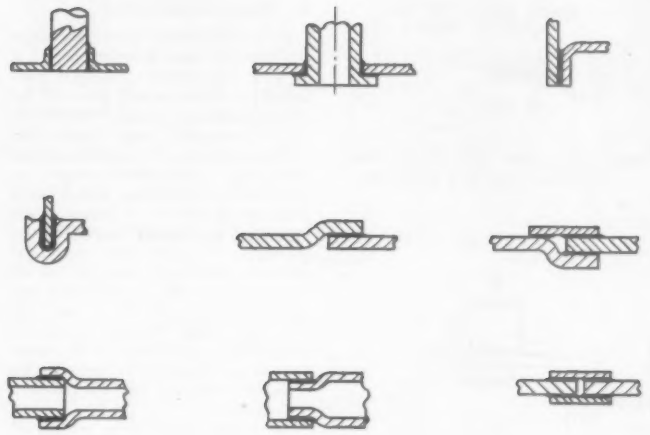
(*a*) Where one part fits inside another, the expansion of the inner part may be such that when brazing temperature is reached there will be a metal-to-metal contact. Thus the proper clearances (Table 2) have not been maintained and little, if any, brazing filler metal will enter the joint and only a fillet will be formed. Transposing the metal parts may produce the opposite condition, increasing the clearances, and may prevent capillary attraction.

(*b*) Assuming the brazing filler metal

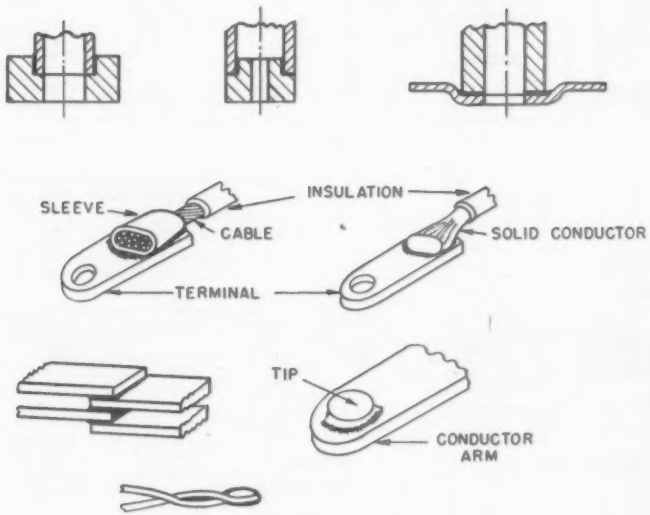
Table 2—Recommended Joint Clearances at Brazing Temperature

Filler metal, AWS-ASTM classifications	*Joint clearance, in.**
BAlSi Group	0.006–0.010†
	0.010–0.025‡
BCuP Group	0.001–0.005
BAg Group	0.002–0.005
BCuAu-1, BCuAu-2	0.002–0.005
BCu	0.000–0.002§
BCuZn Group	0.002–0.005
BMg	0.004–0.010
BNiCr	0.002–0.005
BAgMn	0.002–0.005

* In the case of round or tubular members this means a clearance on the radius.
† Length of lap less than $^1/_4$ in.
‡ Length of lap greater than $^1/_4$ in.
§ For maximum strength use 0.000 in. clearance or press fit.



TYPICAL JOINTS FOR BRAZING



TYPICAL JOINTS FOR BRAZING

Figure 5

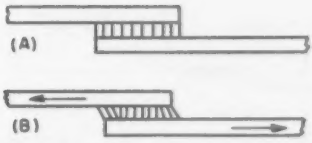


Figure 6 Stress distribution in a brazed lap joint

does enter the joint, a greater contraction of the inner part can result in fracture of the filler metal in the joint during cooling.

2. Stress Distribution. Figure 5 shows typical joints which may be used where normal mechanical strength is required. It must be recognized that, wherever possible, lap joints are preferred; butt and scarf joints should be used only where lap joints cannot be tolerated because of space limitations, etc.

An understanding of the shear stress distribution in a lap joint will result in the design of a joint having maximum strength.

If marks be scribed on the brazing filler metal as Fig. 6 (*A*) and a load applied, the marks will assume the directions and spacings as shown in Fig. 6 (*B*).

This illustrates the fact that the strain (and, therefore, the stress) is higher at the ends of the joint. Another complication is the "couple" or bending moment created by the principal forces being offset from a common axis. This stress at the ends of the joint introduces a tensile stress in the brazing layer and will contribute to the starting of a tear.

It can be concluded from this that where failure occurs progressively, as by tearing, it is evidence of excessive stress concentration at the point where tearing starts. Little may be accomplished by increasing the length of overlap.

The design of a joint should be such as to avoid stress concentration that causes tearing. This can frequently be done by imparting flexibility (reduced rigidity) to that part of the assembly where joint failure is apt to start. Alternatively, the flexible part may be stiffened at the joint. Figures 7 through 13 illustrate correct and incorrect design for the loadings shown.

Note in Fig. 13 that for applications where vibration or fatigue of parts is a factor, the parts should be supported mechanically beyond their brazed and annealed portions.

3. Face Feeding and Preplacement of Brazing Filler Metal. The joint should be designed to accommodate the manner in which the filler metal may be introduced into the joint.

Normally, where joints are being brazed manually, the brazing filler metal is face fed and little or no problem exists. However, where there is enough production to warrant the use of automatic equipment for brazing or because of special requirements, then the preplacement of the brazing filler metal must be taken into account in the design of the joint.

The brazing filler metal may be preplaced in the form of wire, shim or strip, powder, etc. Of these, wire and strip are perhaps more generally used.

Figure 14 illustrates a number of ways in which wire may be preplaced.

Note that in cases where a groove is cut in the base metal for preplacing wire such as in Fig. 14 (*B*) and 14 (*C*) that the heavier section is grooved in each case.

The brazing filler metal leaves the groove to distribute itself throughout the joint as shown in Fig. 15. Thus it is essential to subtract the grooved area from the joint area in designing for mechanical strength.

It is important in the design of a joint where shim or thin flat sections of brazing filler metal are to be preplaced in the joint that the parts be permitted to close up the clearance by applying pressure to the joint during the brazing cycle, as shown in Fig. 16 (*A*) and 16 (*B*).

Thus, excess filler metal and flux is squeezed out of the joint by the proper applications of pressure during the brazing cycle.

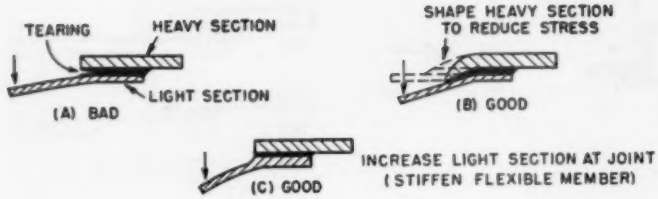


Figure 7

Figure 8

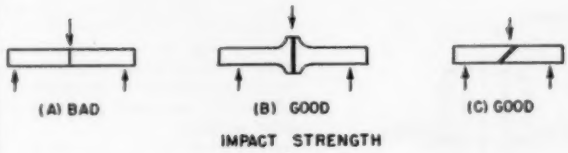


Figure 9

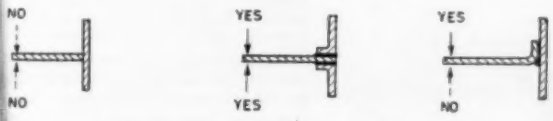


Figure 10

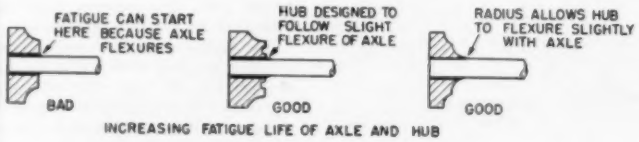

FATIGUE CAN START HERE BECAUSE AXLE FLEXURES
BAD
HUB DESIGNED TO FOLLOW SLIGHT FLEXURE OF AXLE
GOOD
RADIUS ALLOWS HUB TO FLEXURE SLIGHTLY WITH AXLE
GOOD
INCREASING FATIGUE LIFE OF AXLE AND HUB

Figure 11

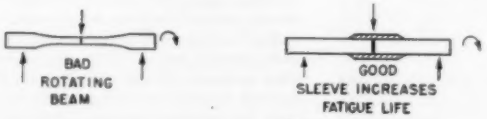


Figure 12

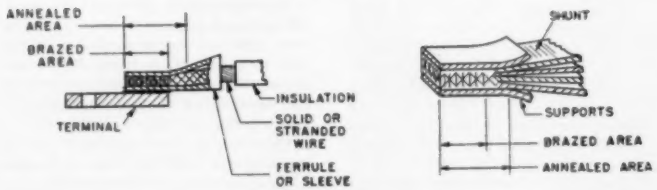


Figure 13

B. Electrical Conductivity

The principal factor to be considered in the electrical design of a joint is that of electrical conductivity. The joint when properly designed should not add appreciable resistance to the electrical circuit.

It is recognized that brazing filler metals in general have very low electrical conductivity as compared to copper. As an example BCuP-5 has approximately 10% the conductivity of copper, BAg-6 approximately 24.4% of the conductivity of copper. A brazed joint should not add any appreciable resistance to the circuit provided the recommended joint clearances shown in Table 2 are used. The shorter path through the brazing filler metal, as compared to the longer path through the conductor results in only negligible increase in the total resistance in the circuit.

As an illustration let us assume the worst case where we have a 1-sq in. cross section of copper with a butt joint through the center as in Fig. 17. We will use BCuP-5 brazing filler metal with a joint clearance of 0.005 in. as recommended in Table 2.

R_{Cu} = Resistance of copper, IACS at 20° C = 6.79×10^{-7} ohms per in./in.^{2}

R_{BF} = Resistance brazing filler metal = $(1/C_{BF})R_{Cu}$

where

C_{BF} = Conductivity brazing filler metal = approximately 10% for BCuP-5 brazing filler metal.

Therefore:

$R_{BF} = (1/0.10)6.79 \times 10^{-7} = 67.9 \times 10^{-7}$ ohms per in./in.^{2}

l = Joint clearance = electrical path through joint = 0.005 in. for BCuP-5 brazing filler metal

Let L = Length of copper conductor + length brazed butt joint. Assume L = 1.000 in. Then

Total Resistance = $R_T = (L - l)R_{Cu} + lR_{BF}$

$R_T = (0.995)\ 6.79 \times 10^{-7} + (0.005)\ 67.9 \times 10^{-7}$

$R_T = 7.10 \times 10^{-7}$ ohms per in./in.^{2}

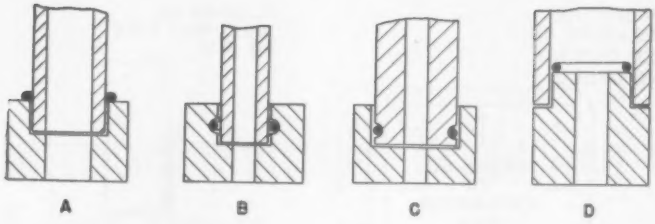
Percent resistance raised by butt joint in 1 in. of conductor =

$$\frac{R_T - R_{Cu}}{R_{Cu}} \times 100 = 4^1/_2\%$$

Therefore the resistance is raised by the butt joint approximately $2^1/_4\%$ in 2 in. of copper, etc.

From a practical viewpoint it must be recognized that there will be a certain amount of voids, etc., thereby cutting the effective area for an electrical path. For this reason, lap joints are recommended where design will permit.

A lap length of $1^1/_2$ times the thickness of the thinner member joined will give a resistance approximately equal to an equal length of solid copper. See Fig. 18. This is a rule of thumb which has been used extensively with good results. A longer lap may be used as necessary for convenience or for some special reason.



PREPLACEMENT OF BRAZING FILLER METAL IN WIRE FORM

Figure 14

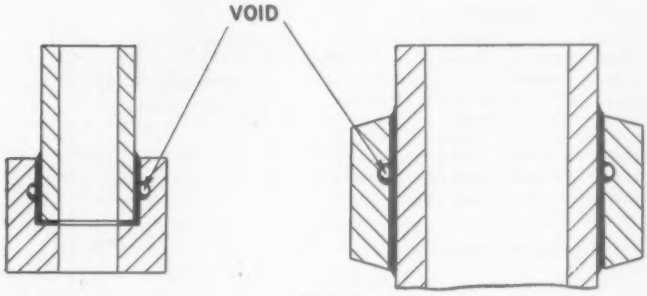


Figure 15

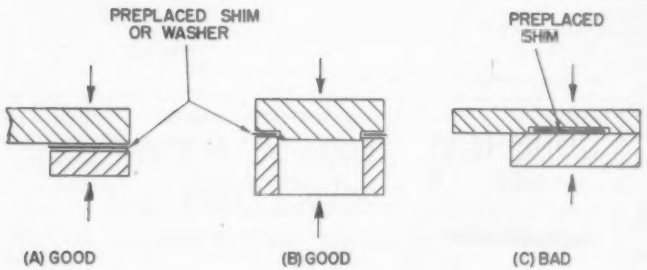


Figure 16

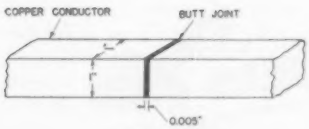


Figure 17

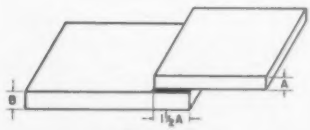


Figure 18

C. Pressure Tightness

It is advocated, where applicable, that brazed joints in cylindrical bodies, hollow containers, etc., be designed in accordance with rules already established for pressure vessels, such as the ASME Code for Unfired Pressure Vessels, etc. Such rules are based on factors of safety of 4 or 5 and the joints are dimensioned accordingly. For low pressures or vacuum, joint proportions can safely be reduced.

Joints in these assemblies should be of the lap (shear) type whenever possible, as this not only gives the strongest joint but it provides a larger brazed area (interface) with correspondingly less chance for leaks through the joint. Figure 19 illustrates a few examples.

The principal factor other than strength to be considered in the design of pressure or vacuum tight assemblies is the proper venting.

The heat from the brazing operation expands the air or gases in the assembly so rapidly that unless the assembly is well vented (open to the atmosphere), it is likely to be forced apart. At the same time forces act on the filler metal entering and flowing through the joint and tend to minimize the effect of capillary attraction. See Fig. 20.

Dead end holes may be considered as small pressure containers. Thus, Fig. 21 illustrates means of designing the joint with vents to eliminate build-up of pressure during the brazing cycle.

Figure 19

D. Elevated and Subzero Service Temperatures

All metals and alloys tend to lose strength as temperature increases. Brazing filler metals are no exception. It can readily be understood that hard and fast rules on the maximum operating temperatures that a brazed assembly will stand is necessarily complex when so many filler metals and base metals are involved. This, coupled with the fact that maximum temperatures may be involved for a relatively long or short duration, is the reason why the subject is treated in a very general manner here.

1. Elevated Service Temperatures. Table 3 gives maximum operating temperatures for each particular type of filler metal and is based on continuous service at this temperature. It is assumed that the base metal is suitable for the service temperatures, and that the proper filler metal will be selected according to the base metal used.

Many of these filler metals will withstand much higher service temperatures for short periods of time with reduced

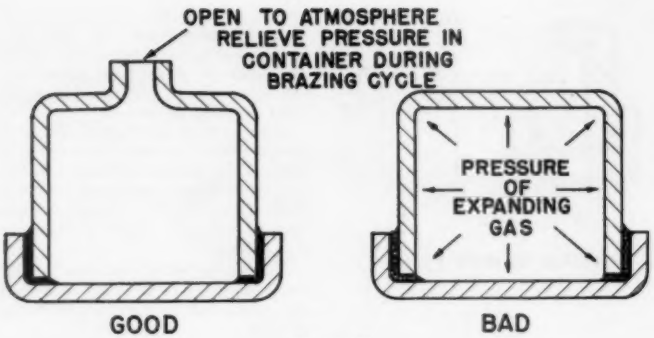


Figure 20

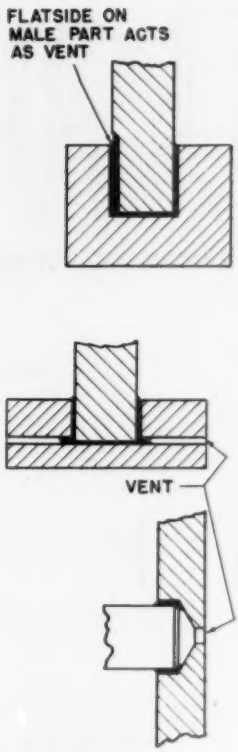


Figure 21

Table 3

AWS-ASTM classification	*Suggested maximum service temperature, ° F*
BCuP Group	300
BAg Group	400
BCuZn Group	400
BCu	400
BAgMn	500
BNiCr	1000

stress requirements. It is thought at this time, however, that when applications warrant service temperatures other than those indicated, it is advisable to adequately test applicable samples to determine whether the brazing filler metal chosen will meet the necessary requirements of time, temperature and stress involved.

2. Subzero Service Temperatures. Present data indicates that tensile, shear and impact properties of brazed joints are not adversely affected to a temperature of at least −100° F. as compared to these properties at room temperature.

When in doubt, applicable samples should be tested to determine whether or not the necessary requirements of time, temperature and strength can be met by the joint.

ACKNOWLEDGMENTS

The author wishes to express his appreciation for the helpful comments and suggestions offered by L. H. Hawthorne, Revere Copper and Brass Inc.; C. E. Johnson, Scaife Co.; R. M. Wilson, Jr., International Nickel Co., and all other members of the AWS Committee on Brazing and Soldering.

Increased Multispot Production with Limited Power Supply

A new multispot control provides increased welding production in spite of limited power supply by reducing transients and sequence firing transformers in small groups

by C. R. Whitney, Jr.

HISTORICAL

A NUMBER of years ago the constant attempt to attain higher, more efficient production provided the impetus for development of a resistance-welding machine capable of making several spot welds simultaneously. Lending itself very readily to automotive, refrigeration and other mass production industries, the "multispot" idea caught on quickly, and today it is considered as important as gun welding in many applications.

Originally, the large size of welding transformers usually necessitated using only one welding transformer on the multispot machine. The method of transferring the welding function from one electrode to another was done in two different ways. One was to connect all of the electrodes in parallel to the welding transformer. The mechanical sequencing was arranged so that only one electrode was seated on the work at any one time. Thus, only one weld was made each time the transformer was fired. Although inflexible and relatively slow, an advantage was often realized with this machine since the clamping and fixture positioning operation had to be done only once, while any number of welds could be made.

The other original type of machine also had all of the electrodes connected in parallel to the same welding transformer secondary. In this type, however, all of the welding electrodes were closed at the same time. The secondary circuit of the transformer was switched from one set of electrodes to another by means of a bar commutator. The actual time that welding current flowed through any one set of electrodes was determined by the time required for the commutating device to move from one segment to the next. The inflexibility, inaccuracy and frequent commutation trouble encountered with this machine left much to be desired.

Subsequent improvement in the quality of transformer iron and more efficient transformer design led to an important modification of the basic multispot welder design. Transformer size for a given capacity was reduced to the extent that it became practical to provide separate transformers for each set of welding tips. Welding transformers with two secondary circuits are now manufactured expressly for multiple-spot machines. Each secondary circuit normally feeds two welding electrodes in series, so that each transformer is capable of making four spot welds simultaneously.

Development of control panels specifically designed for the multiple-spot type of welder was not done originally. Sequencing relays, hydraulic pump motor starters, power switching contactors, sequence weld timers, etc., were purchased as individual units, mounted at random, on the welding press machine, and interconnected with a myriad of control wires. This system worked satisfactorily at the start, but as the number of operations on a machine increased, and breakdowns began to occur, the economic advantage of the multiple-spot machine was threatened by long, costly down-time for maintenance and repair. The necessarily complex system of control interwiring was, in many cases, further complicated by numerous changes made on the job, which were seldom properly recorded on wiring diagrams.

Troubles such as these can now be greatly minimized by the use of unit control panels...i.e., complete, prewired control units, including machine sequencing and weld timing control in a single enclosure. This type of control relieves the user and welding machine manufacturer of the responsibility of control circuit design, since the units can be completely designed and manufactured by experienced control manufacturers. Maintenance problems can be considered with the control panel in the design stage so that components are made more easily accessible and replaceable, and interwiring is greatly simplified. Accurate diagrams can be furnished and the temptation to overlook the task of correcting them for wiring changes made in the field is minimized.

C. R. Whitney is connected with the Square D Co., Milwaukee, Wis.

Paper presented at the Thirty-Third Annual Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Jan. 15, 1953.

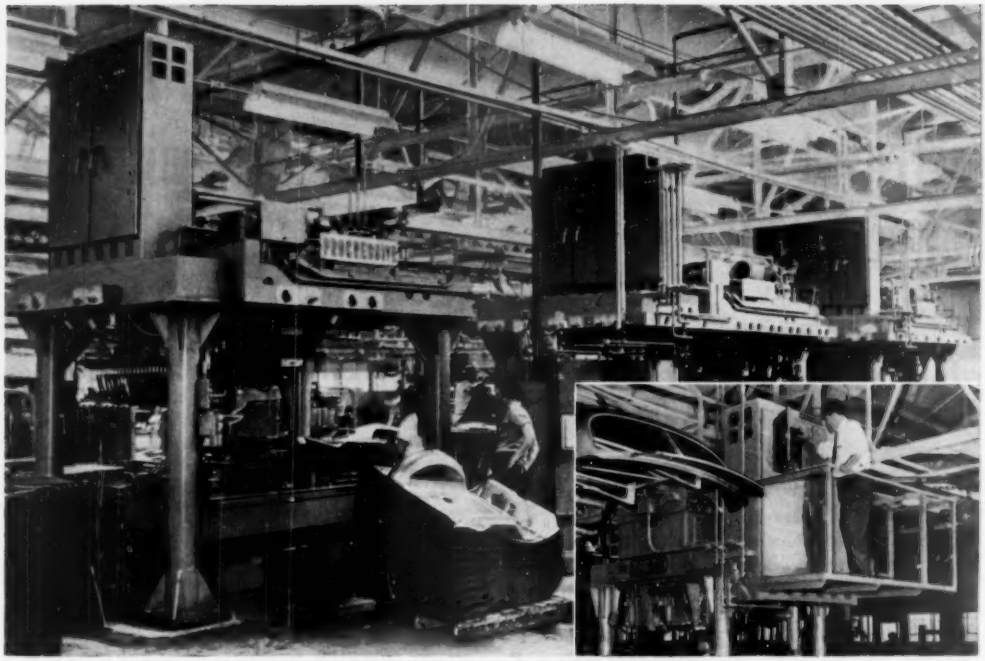


Fig. 1 Typical installation of multiple-spot welding presses and control of the type discussed in this paper

The advantages of this type of construction have been recognized by the Joint Industry Conference (JIC) and National Machine Tool Builders Assn. (NMTBA). JIC specifications require, and NMTBA specifications recommend, that all control equipment for a single machine be mounted in a single enclosure.

DEVELOPMENT AND DESIGN

The control equipment discussed in this paper represents another progressive step in the improvement of welding processes and machines. Heretofore little consideration has been given to transient and power supply problems in multiple-spot machines. Circuits and equipment have been more complex than necessary to do the job, and the adaptability of last year's machine to this year's model change has been limited.

The prospective user of this control presented the control manufacturer with the following requirements:

1. Power demand of the machine was to be kept below 1000 amp on any line.
2. Transients were to be kept to a minimum.
3. Welding transformer primaries were to be disconnected from the line when the machine was not actually in use.
4. Sequencing of all machines was to be standardized from the operator's standpoint.
5. Control panel was to be constructed with due consideration for ease of maintenance and flexibility of design.

The first requirement, that of keeping power demand to a minimum, was of major importance. Considering an existing welding power supply, and knowing the number and size of machines to be installed, the user requested that no machine, regardless of its size, draw more than 1000 amps per line at any time. In exploring the problems of this requirement, let us consider one of the machines pictured in Fig. 1. This machine utilizes forty-two 40-kva welding transformers, each of which has two secondaries and supplies four welding tips. The series welding method employed gives four spot welds per transformer. The machine, therefore, makes 168 spot welds each time it cycles.

Each 40-kva welding transformer draws approximately 250-amp primary current during welding. With a maximum of 1000 amp allowable, only four of the 42 transformers could be fired simultaneously. The transformers, therefore, were divided into groups of four or less, and these groups were fired in sequence.

The conventional method of separating the groups or "firings" has been to provide a separate ignitron contactor for each. This method is undesirable, however, since it is relatively costly and requires an excessive amount of space. A better method was sought in designing this control. The solution adopted was to use only one ignitron contactor per phase but connect different groups of welding transformer primaries to the ignitron contactor for each firing. The switching was performed by relatively small magnetic con-

tactors which were electrically interlocked to prevent firing more than one group of four transformers at one time.

Although basically single phase, speed of production was enhanced and load balance approached by dividing the transformers on three phases. This division required the use of three ignitron contactors (one for each phase), but the additional cost was justified on machines of 15 transformers or more.

At the beginning of the three-phase machine cycle, two switching contactors were energized. One of these contactors connected transformers 1, 2, 3 and 4 on Phase A, and the other connected 5, 6, 7 and 8 on Phase B. After the initial stages of the machine sequence, weld timer No. 1 fired the Phase A ignitron contactor, thereby energizing transformers 1, 2, 3 and 4 through the first switching contactor. At the end of this weld time, weld timer No. 2 fired the Phase B ignitron contactor energizing transformers 5, 6, 7 and 8 through the second switching contactor. During weld time No. 2, the first switching contactor (Phase A) was de-energized and another switching contactor energized, the latter connecting transformers 9, 10, 11 and 12 on phase C. In this manner, the switching contactors were picked up during the weld time preceding the one with which they were associated, and dropped out during the weld time following the one with which they were associated, with no loss of production time and no danger of closing or opening the contacts under load. The firings continued, four transformers per firing, sequenced on Phase A, then B, C, A, B and so forth until all of the welds were made.

A single-phase machine would have a similar sequence, the main difference being the necessary addition of several small relays to sequence the switching contactors between weld times. As with the three-phase machine, the switching contactors are not required to make or break the welding current.

Since the switching contactor serves only to carry the welding transformer primary current, and not to make or break the circuit, the determination of the contactor size depends solely on heating due to I^2R losses.

Heating of contactor parts is proportional to the square of the current multiplied by the time during which the current flows. For any given intermittent duty cycle and current value it is possible to calculate the value of current which would cause the same heating effect if flowing continuously. This "equivalent continuous current" may be compared to the 8-hr amp ratings of standard contactors to determine proper size selection. Calculation is as follows:

$$I_p^2 t_w = I_0^2 t_0$$

$$I_0^2 = I_p^2 (t_w/t_0)$$

$$I_0 = I_p \sqrt{t_w/t_0}$$

Where

I_p = primary current, amp.
I_0 = equivalent continuous current, amp.
t_w = actual time duration of welding current during interval used for calculating contactor duty cycle.
t_0 = interval used for calculation of contactor duty cycle.

Also note that

$t_w/t_0 \times 100$ = duty cycle expressed in percent.

Therefore:

$$I_0 = \frac{I_p}{10} \sqrt{\% \text{ duty cycle}}$$

Returning to the example, the machine in question was to be operated 100 times per hour at 20 cycles maximum weld time. Since each transformer was to be fired only once during a press operation, the duty cycle was:

$$\frac{t_w}{t_0} = \frac{120 \text{ operations/hour} \times 20 \text{ cycles/operation}}{60 \text{ cps} \times 3600 \text{ sec/hr}} = \frac{2400}{216{,}000} = 0.0111 \text{ or } 1.11\%$$

The equivalent continous current

$$I_0 = I_p \sqrt{0.0111} = 0.105 I_p$$

Where I_p = primary current = 250 amp (40-kva welding transformer). Therefore

$I_0 = 0.105\ (250) =$
26.3 amp equivalent continuous current

From a heating standpoint, therefore, a 50-amp contactor could be used, even though it is actually carrying 250 amp for short periods of time.

The second requirement of this controller was to reduce current peaks in order to lengthen welding equipment life. Difficulty had previously been experienced with large transient surges of current causing tripping of circuit breakers and premature failure of welding cables and transformers. In view of this trouble, something had to be done to reduce transients.

If they must absorb transient surges, cables and transformers must be designed larger than is necessary to do the welding job. Cables and transformer windings are subjected to mechanical flexing caused by the magnetic fields surrounding them. If transient surges are drawn by the welding transformer, the strength of the magnetic field is increased proportionately, thereby increasing these mechanical stresses. When random starting of the weld causes the most severe transient conditions, saturation of the transformer iron may occur and the primary current peaks become extremely high. If these transients are minimized, better core materials and higher flux densities may be utilized without experiencing serious transient saturation. Thus transformers can be made smaller and less expensive.

In the face of these problems, the logical solution seemed to be to minimize the transients rather than design the equipment unnecessarily large to accommodate them. Transient reduction presented somewhat of

a problem, however, because pneumatic timers were selected to perform the timing functions. Reasons for the choice of pneumatic timers are given below. To reduce the initiation transient, it is necessary to fire the ignitrons at the proper point on the voltage wave. However, a pneumatic timer knows nothing of the status of the current and voltage wave at any particular instant.

It occurred to the designers that rather than connect the pneumatic timer contacts directly to the ignitron tube ignitors, an intermediate stage could be added. This intermediate stage would intercept the random signal from the pneumatic timers, and "refine" it to the extent that the ignitron contactors would be energized in the proper manner.

Another function of the intermediate stage (or synchronizing panel as it became known) was to insure that the welding transformer conducted only complete cycles of current. This function is known as "lead-trail" action and is important to avoid transformer saturation when short weld times are used. Saturation of the iron causes loss of impedance in the transformer primary, so that the windings become practically a short circuit. The resultant current can be of such magnitude as to cause the same sort of strain on transformer windings and cables as is experienced when heavy initiating transients occur.

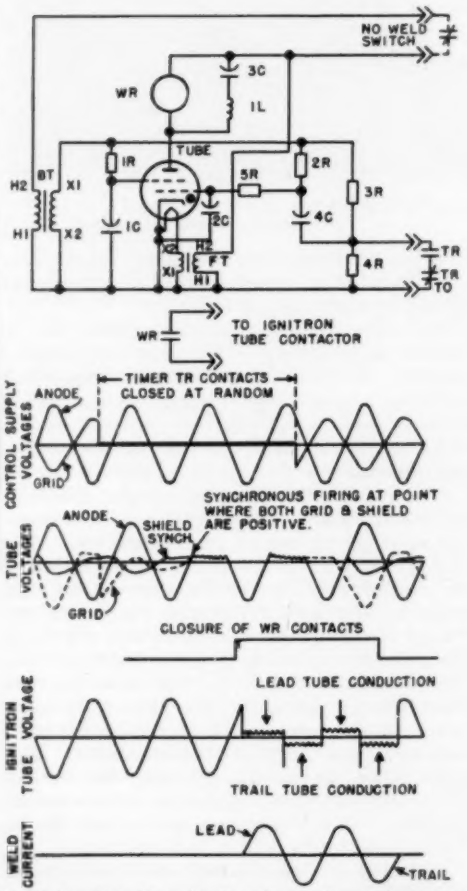


Fig. 2 Synchronizing circuit and wave shapes illustrating two-cycle weld time. Horizontal axis of wave shapes shows time relation of occurrences between random closure of pneumatic timer and synchronized firing of ignitron tubes

The synchronizing relay circuit and a chart showing voltage wave forms with respect to time is shown in Fig. 2. Operation is as follows: energization of the supply allows warm-up of the thyratron tube heater. As the cathode heats, half wave current is drawn from bias transformer *BT* by the control and shield grids of the tube to establish, by self-rectification, the proper bias voltages and prevent anode conduction.

The control grid hold-off voltage consists of a-c opposite in polarity to the anode supply voltage appearing across *4R* of the *3R*, *4R* voltage divider and transmitted through *4C* and *5R*. On this is superimposed the negative voltage of the charge drawn into the hold-off capacitor *4C* due to self-rectification at the control grid. The composite hold-off voltage wave is such that the grid is negative at all times except when the anode is safely negative.

The field grid voltage is a distorted a-c synchronizing voltage with positive loops cut off by its self-rectification, and lagging the a-c bias supply voltage. The shield grid becomes negative shortly after the beginning of each positive anode half cycle and remains negative until after the anode voltage has become negative.

Energization of the *TR* timer immediately closes *TR* interlock contacts, short circuiting the a-c hold-off voltage across *4R*. Voltage at the control grid becomes mainly the rapidly decaying negative d-c across *4C* which discharges through *2R* and *3R*. On this is superimposed a small a-c component fed in by *2R*, lagging the out of phase bias supply and tending to drive the grid slightly positive at the beginning of each anode positive half cycle. As the grid hold-off voltage disappears, the tube is prevented from firing late by the synchronizing voltage on the shield grid, which is negative during all but the first few electrical degrees of each positive anode pulsation. If the signal from the pneumatic timer is received after the shield grid becomes negative, the tube waits to fire at the beginning of the next succeeding positive anode half cycle, thus insuring only complete half wave pulses of current through *WR* firing relay coil.

Firing relay *WR* is thus energized synchronously, by the tube conduction. Design of this relay is such that its contacts close with very little bounce and at a consistent point with respect to the line voltage wave. The charge drawn into *3C* during half-wave conductions of the tube serves to keep the relay coil energized during negative half cycles when the tube is not conducting. Reactor 1L produces a damped oscillation of the discharge of *3C* after the tube stops conducting at the end of the weld time. This oscillation provides rapid de-

magnetization of the relay coil for fast and accurate drop-out.

With the tube supply connected to the same phase as the welding transformer load, the mechanical time lag of the relay in closing causes the ignitron contactor to be fired at the proper instant for minimum transient. It has been found that factory adjustment of the mechanical time constant to the value needed for a typical, average machine will always minimize transients to the extent that they are no longer a problem, regardless of slight power factor variations between specific machines.

At the end of weld time, opening of the timed *TR* contact removes the short circuit across *4R*. Upon termination of any tube conduction in process, *4C* will rapidly recharge during the succeeding negative half cycle. This, together with reappearance of the a-c bias voltage, restores hold-off voltage at the control grid which prevents further tube conduction. The time lag of the weld relay in opening after the last tube conduction is designed to insure breaking of the ignitor circuit just before a positive half cycle of line voltage. This insures that the last half cycle of weld current is always negative and of opposite polarity from the first half cycle, thus providing full cycles of conduction and minimizing any chance of welder transformer saturation.

The third requirement was important as a safety feature. An ignitron contactor ordinarily breaks only one power line to each welding transformer, while the other remains connnected at all times. Impurities in the ignitron tube cooling water will allow conduction of small amounts of current through the welding transformer primary even when the tubes are not "firing." Actual measurement has shown voltages up to 200 v across the primary windings on a 460-v line. During this off time, the transformer windings are cooling rapidly (since they are not carrying welding current). This is an unfavorable combination of occurrences. Forced cooling of the transformers by water circulated through the secondaries very often results in moisture condensation. Moisture on the windings weakens the insulation, and the 200 v provides the potential to hunt for a leakage path and break down insulation. In addition, the leakage voltage presents a possible hazard of electrical shock to the operator.

The solution was relatively simple. It consisted of a magnetic line contactor which closed at the beginning of each machine cycle, and opened all power lines at the end. Choosing the size of this magnetic disconnect was much the same problem as that of choosing switching contactor sizes, except the duty cycle is that of the complete machine, and the current is that in each power line rather than the load current of an individual group of transformers.

The next requirement, that of standardized machine sequence, was of great importance from an operational standpoint. Any operator must be able to step to any one of the machines and make it weld properly without special training regarding operation of the controls. The circuit, therefore, was designed so that certain functions occurred on each machine when the Start, Emergency Stop, Reverse, etc., buttons were depressed. Depressing the Start button closed a vent valve which closed the drain and caused the hydraulic pressure to build up in the system. As soon as the pressure built up, the platen or table would raise into welding position. When the platen reached the welding position, the automatic control took over and the operator could release the Start button. At this point, the hydraulic locks closed to hold the work and platen in place, and the welding tips advanced. After sufficient pressure built up on the welding tips, the actual welds were made in the manner described previously. Upon completion of the welding, the tips retracted and locks were released simultaneously. After a "platen delay time," the platen returned to its quiescent position and a stripper unseated the workpiece from the platen so that it could be removed easily and a new piece placed in position.

With the proper choice of valve types, the control was designed so that the machine could be stopped at any point in the sequence, and the only subsequent motion would be retraction of the welding tips. The machine could then be returned to its quiescent position by depressing the Reverse button. In addition, each control was designed so that the operator could perform each individual function (raise the platen, advance the welding tips, and "fire" the welding current) by manually depressing the Start button and operating a selector switch.

As the building blocks fell together, the fulfillment of the last requirement became a reality, for ease of maintenance and flexibility of design was considered in the choice of each component. Pneumatic timing units rather than electronic were chosen because of the relative ease of adding timing periods or modifying the timing circuit. At the same time, the accuracy, ruggedness and dependability of the pneumatic timers appealed to the user. Magnetic switching contactors went hand in hand with the timers, with the result that the addition of one pneumatic timer and one 4-pole switching contactor allowed the addition of up to four welding transformers or 16 spots. The necessary wiring for a modification such as this was also very simple. All moving parts of the timers and contactors were case hardened to increase life.

To facilitate their quick removal and replacement, the synchronizing panels were mounted on four studs and the wiring "plugged" into the timer panel. If necessary, the complete timer panel could be easily removed from the door without the necessity of tagging a myriad of wires. All harness wires were run through a separable connector so that they automatically lined up with the proper terminal when the panel was removed and replaced.

Space was provided on the relay and timer panel for future expansion if necessary. Testimony of the success of this design is that the actual controller pictured in Fig. 3 was originally designed for 168 spots

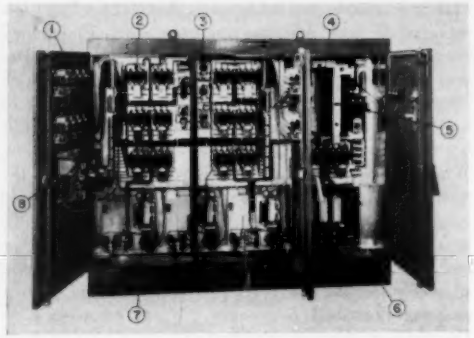


Fig. 3 Multispot controller showing (1) pneumatic timers, (2) switching contactors, (3) machine sequencing components, (4) main circuit breaker, (5) pump motor starter and circuit breaker, (6) magnetic disconnect, (7) ignitron contactor panels and (8) electronic synchronizing panels

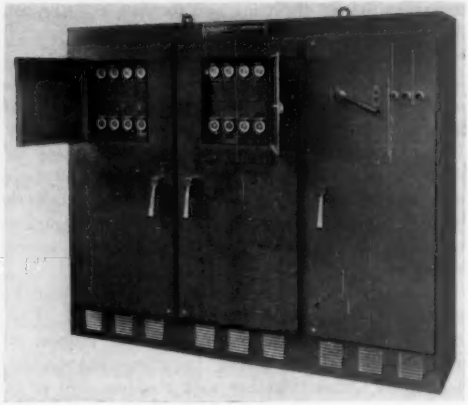


Fig. 4 Figure 3 controller with main doors closed, illustrating timer access doors, circuit breaker operating handle and control pilot lights

per operation. After installation, however, the user converted this controller to make 200 spots per operation with no assistance from the control manufacturer. Changes such as this are made easier by use of a separate pneumatic timing device for each group or firing of welding transformers. No changes are needed in electronic equipment or circuits.

Physical construction of these controllers also provided several maintenance features. Panel-type wiring with all terminal points well identified makes circuit tracing a simple job. The grouping of all components (pump motor control, circuit protection, machine sequencing, weld sequencing and power disconnect) in the same enclosure simplifies the maintenance problem as mentioned earlier and is in accordance with JIC and NMTBA specifications. Plug-in synchronizing panels and individually removable timing relays are easy to exchange so that tests and repairs may be made on the bench rather than on the machine.

SUMMARY

The final result is illustrated in Fig. 3. In this photograph are shown the pneumatic timing relays, power switching contactors, machine sequencing relays, circuit breakers, pump motor starter, magnetic disconnects, ignitron contactors and synchronizing relay panels. Each of these components has now proved its ability to perform its vital function in the process of making high-quality, high-production welds on the multitransformer-type welding machine.

At a recent count, the control pictured in Fig. 3 has welded sections of more than 175,000 automobile bodies. At a rate of 200 welds made on each body, this represents a total of over 35 million welds with no recorded down-time charged to the control. To substantiate this, the author spent a good part of a day searching through the downtime record book at the user's plant. In addition to the above statistics, there was a definite agreement that cable and transformer life on the machines using the control described in these pages was much better than that previously experienced with other types of control. It was also noted that the transient-free weld initiation resulted in longer useful electrode life and less frequent dressing. This effect is similar to that which might be expected from the use of slope control.

More than 70 controllers of the design described have been installed and are operating successfully. They have been used with a wide variety of welding machines ranging in size from 8 to 42 transformers. Several have involved such additional requirements as fixture shifts, platen shifts, welding more than one spot with a single electrode and many other complicated hydraulic sequences. In all cases, the results obtained with the control herein described have been excellent.

Forming and Welding of Titanium

Forming and welding techniques of titanium used in rocket components

by Francis H. Stevenson

TITANIUM, a relatively new metal, possesses physical properties which are very desirable in rocket components. However, methods of fabricating and welding titanium are not well known. It is the purpose of this paper to discuss some forming and welding techniques used by the Aerojet Engineering Corp.

Commercially pure titanium, purchased from the Titanium Metals Corp of America and designated Ti-75A, was used in all of the tests performed in this investigation. The material was bought under a specification requiring an ultimate tensile strength range from 70,000 to 100,000 psi with a minimum elongation of 15% in a 2-in. gage length. The material received was at the extreme upper range of the specified tensile strength and had minimum permissible elongation. This material was the most unfavorable titanium with which to perform stretch tests. The severity of the stretch forming required for the particular part thus provided an extremely difficult problem.

Because the sheets might have been hardened by cold rolling or stretcher leveling, tensile specimens were annealed and tested to determine whether annealing would lower the strength and increase the ductility. The results, given in Table 1, indicated that the abnormal strength was not due to cold work, but to an excessive amount of one or more of the alloying elements, as neither strength nor ductility was changed appreciably by annealing.

Stretch-forming tests were made under the following conditions:

1. Ambient temperature without interstage annealing.
2. Ambient temperature with interstage annealing.
3. Elevated temperature without interstage annealing.
4. Elevated temperature with interstage annealing.

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Table 1—Elongation and Ultimate Strength of Specimens

Specification No.	*Anneal temperature, ° F*	*Ultimate strength, psi*	*Elongation % in 2 in.*
1	As-received	102,450	15.5
2	900	104,800	15.5
3	1350	104,000	15.0

An Erco stretch press of 150,000-lb capacity and a Kirksite die were used for all tests. The interstage annealing temperature used in all tests was 1350° F for 10 min. On Tests Nos. 1 and 2, Keystone grease No. 84 was brushed over the die each time a blank was removed and replaced. When interstage annealing was used, it was found that after the fourth annealing it was necessary to pickle the blank in order to avoid jaw slippage. Pickling was done in Heat Bath Corp. "Scale Kleen" for 5 min at 850 to 900° F. The blank was then water-quenched, pickled in 20% H_2SO_4 for 5 min, rinsed in water and brightened in 7% HF, 46.5% HNO_3 (38° Baumé) for 5 min. The blank was given a final rinse in water and exposed to a high-pressure spray to remove loose scale and acid. During Tests Nos. 3 and 4, the die was maintained at 465 ± 10° F. Temperature was measured by a thermocouple adjustable to measure the temperature of either die or blank, using a portable potentiometer. The blank was painted with a molybdenum disulfide lubricant (Allube CSM No. 498, a product of the Allube Corp., Glendale, Calif.).

Figure 1 shows titanium sheet in the stretch press. The four large tubes in the die contain the heaters during forming. The small tube in the center was used for the thermocouple; this tube extended to the upper surface of the die so that the temperature could be taken at the surface or inside of the die.

During Test No. 1, pressure was applied slowly to give a ram speed of approximately $^1/_8$ in. per minute. Failure occurred at 125,000 lb ram force, with the crack running the full width of the blank at a distance of 20 in. from one end. The average elongation over the entire sheet was 10.2%, and the maximum in a 6-in. gage length was 18.8%. In Test No. 2, the same ram speed was used as in Test No. 1. The sixth time the blank was annealed and pulled, failure occurred in the same place as before. The average stretch over the entire length was 16.7%. In Test No. 3 the die was

Fig. 1. Titanium sheet in stretch press

heated and the blank was clamped in place and stretched slightly to gain as much contact with the die as possible. All parts of the blank not in contact with the die were heated by oxy-acetylene preheating torches to 800° F, the temperature being determined by the use of Tempilstiks. An asbestos blanket was then thrown over the blank and ram pressure applied. Pressure was released at intervals to measure the amount of stretch and to heat the edges of the blank. A tear 4 in. long appeared approximately 12 in. from one end after an average stretch of 23.6% was obtained. The maximum stretch in a 6-in. gage length was 33%. Test No. 4 was conducted in the same manner as Test No. 3 except that the blank was removed and annealed at intervals. Failure occurred 12 in. from one end after an average stretch of 38% and a local stretch of 73% were obtained.

Figure 2 shows the sheet partially formed and the edge of the failure.

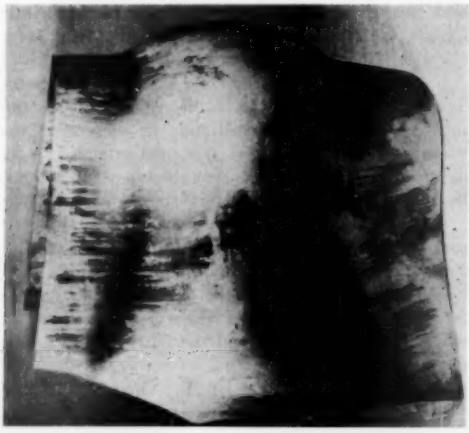

Fig. 2 Sheet partially formed and edge of failure

Fig. 3 Sheet fully stretched

Figure 3 shows the sheet fully stretched.

Spinning was another method of forming which Aerojet Engineering Corp. investigated. All spinning tests were conducted on 0.063-in. sheet. The first attempts were made with both the blank and spinning chuck cold. The material could not be spun to a cup more than $1^1/_8$ in. deep without the edge cracking and buckling. However, by trimming and heating the edges to 400° F it was possible to finish the spinning operation. Various temperatures and chucks were used in subsequent attempts. To spin a $5^1/_2$-in. diam hemispherical head for a tank, the following procedure was used: The blank and first stage chuck were maintained at a temperature of 400° F, which resulted in a cup $1^1/_2$ in. deep. This cup was then put on the final stage chuck; both were maintained at a temperature between 500 and 600° F, and the part was formed without difficulty.

In the stretch-forming tests, the thinning of the material amounted to between 0.010 and 0.020 in. However, this particular part requires a very severe stretch-forming operation, and with adequate tooling and proper heating the thinning could be probably kept to a maximum of 0.010 in.

In the spinning tests, our thinning was held to a maximum of 0.010 in. Other methods of forming titanium are also being used, such as drop hammer, punch press and hydropress. With the use of high-temperature rubbers it is now possible to form the material using rubber platens on the presses. In general, it may be stated that the formability of sheet in the annealed condition approximates that of 302 quarter-hard stainless steel. A bending radius of three to five times the sheet's thickness should be considered as the minimum, to allow for grain direction and surface imperfections.

Most stainless-steel forming techniques can be adapted to the fabrication of titanium parts. The metal should be deformed at temperatures between 600 and 1000° F, depending on the extent of deformation. In tests, lower temperatures have been used

Table 2—Spot-Weld Data with Variable Heat Adjustment

Sequence settings: Squeeze time	Sequence settings: Weld time	Sequence settings: Hold time	Heat settings: Transformer settings	Heat settings: Weld heat adj. in %	Net electrode force, lb	Shear strength, lb
20	2	20	3	70	500	1600
20	2	20	3	70	500	1650
20	2	20	3	72	500	1800
20	2	20	3	72	500	2100
20	2	20	3	74	500	2000
20	2	20	3	74	500	2100
20	2	20	3	76	500	2000
20	2	20	3	76	500	2400
20	2	20	3	76	500	2400
20	2	20	3	76	500	2200
20	2	20	3	76	500	1950
20	2	20	3	76	500	2200
20	2	20	3	76	500	1900
20	2	20	3	76	500	1600
20	2	20	3	76	500	2200
20	2	20	3	76	500	1700
20	2	20	3	76	500	2300
20	2	20	3	80	500	2200

principally because Kirksite dies were involved. It is thought that better formability can be obtained by the use of steel dies and by heating the material to from 600 to 1000° F.

After the parts are formed it is necessary to join them which brings us into a discussion of the welding of titanium. Both resistance- and fusion-welding processes are used. At the present time the only fusion process is the inert-gas shielded-arc method. Of the resistance processes, spot, seam and flash welding are being used with marked success. Considerable investigation has been devoted to the spot welding of titanium, and it is not the writer's intention to go into a lengthy discussion, but only to give some results obtained at Aerojet. The type of spot-welding machine used was a 75-kva, press type using 440-v, 60-cycle, single-phase alternating current. A synchronous control panel was used in conjunction with the welder. The upper electrode tip was a Class 2 with a 2 in. radius. The lower electrode tip was a Class 2 with a $^1/_2$-in. flat. These tips were used throughout the series of spot-welding tests. A group of spot welds were made in which a squeeze time of 20 cycles and a hold time of 20 cycles remained constant. Table 2 shows the spot-weld data for various heat adjustments. Another group of tests were made in which the weld cycle, the major transformer taps and the percent of heat were varied with constant tip pressure. A short welding cycle and high heat were found to be desirable for the particular applications at Aerojet as shown in Table 3. All spot-welding tests were made using commercially pure titanium, Ti-75A, with a thickness of 0.063 in.

Figures 4 and 5 show the types of weld nuggets. Figure 4 shows a spot weld made under the following conditions: squeeze time, 20 cycles; weld time, 2 cycles; hold time, 20 cycles; net electrode force, 500

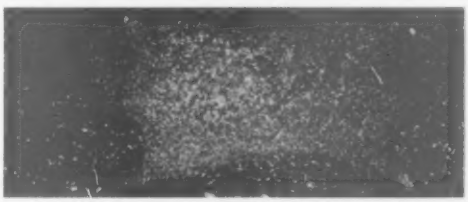

Fig. 4 Weld nugget—heat 80%

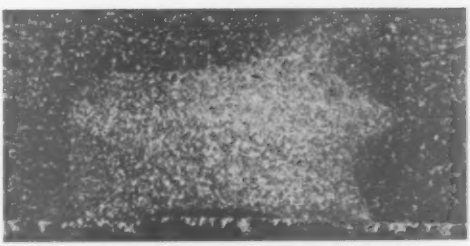

Fig. 5 Weld nugget—heat 74%

* Begeman, M. L., McBee, F. W., Jr., and Fontana, J. C., "The Physical and Metallurgical Characteristics of Spot-welded Titanium," The Welding Journal, 30 (9), Research Suppl., 429-s to 434-s (1951).

Table 3—Optimum Spot-Weld Data and Machine Settings for Aerojet Application

Sequence settings: Squeeze time	Sequence settings: Weld time	Sequence settings: Hold time	Heat settings: Transformer settings	Heat settings: Weld heat adj. in %	Net electrode force, lb	Shear strength, lb
20	4	20	3	70	500	2300
20	4	20	3	70	500	2700
20	4	20	3	70	500	2300
20	4	20	3	70	500	2200
20	4	20	3	70	500	2600

Table 4—Welding Data on Inert-Gas Tungsten-Arc Butt Welds in 0.063-In. Ti-75A Titanium Sheet

Item	Specimen 1	2	3	4	5	6	7	8	9
Filler wire	Ti-75A	None	None	Ti-75A	Ti-75A	None	None	None	None
Wire diam., in.	0.062	..	..	0.062	0.062	..	..	..	..
Current, d c, amp	100	100	100	100	100	50	50	50	50
Arc voltage	..	..	..	..	..	13	13	13	13
Polarity, straight electrode type*	WT	WT	WT	WT	WT	WT	WT	WT	WT
Electrode diam., in.	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$	$^1/_{16}$
Inert gas	Argon	Argon	Argon	Argon	Argon	Helium	Helium	Helium	Helium
Torch, cfh	30	30	30	25	25	25	25	25	25
Backup, cfh	10	10	30	10	None	10	10	10	10
Manual	X	..	..	X	X	..	..	..	..
Automatic	..	X	X	..	..	X	X	X	X
Carriage speed, fpm	..	20	18	..	..	19	19	19	19

* WT = Thoriated tungsten.

Table 5—Weld-Joint Tensile-Test Data for Butt Welds in 0.063-In. Ti-75A Titanium Sheet

Specimen No.	Ultimate load, lb	Ultimate strength, psi	Elongation in 2 in.,	Elongation Percent	Weld to fracture, in.
2A	2400	76,200	0.33	16.5	0.65
2B	2500	75,800	0.33	16.5	0.70
2C	2450	74,900	0.33	16.5	0.85
2D	2450	74,900	0.35	17.5	0.75
Avg	..	75,500	..	16.9	..
3A	2450	74,500	0.31	15.5	0.70
3B	2400	71,800	0.32	16.0	0.80
3C	2450	74,700	0.37	18.5	0.75
3D	2400	73,300	0.33	16.5	0.75
Avg	..	73,600	..	16.6	..
4A	2500	77,600	0.34	17.0	0.65
4B	2500	81,000	0.33	16.5	0.75
4C	2450	75,900	0.33	16.5	0.80
4D	2450	76,000	0.31	15.5	0.75
Avg	..	77,600	..	16.4	..
5A	2510	74,800	0.32	16.0	0.92
5B	2400	75,500	0.31	15.5	0.75
5C	2400	75,500	0.31	15.5	0.70
5D	2450	76,400	0.33	16.5	0.68
Avg	..	75,600	..	15.9	..
6A	2100	78,400	0.34	17.0	0.62
6B	2550	76,800	0.38	19.0	0.44
6C	2600	78,500	0.36	18.0	0.61
6D	2550	77,000	0.36	18.0	0.66
Avg	..	77,700	..	18.0	..
7A	2500	74,400	0.33	16.5	0.70
7B	2550	76,000	0.34	17.0	0.65
7C	2500	74,200	0.33	16.5	0.65
7D	2450	72,500	0.32	16.0	0.70
Avg	..	74,300	..	16.5	..

lb; transformer setting, No. 3; weld heat, 80%; shear, 2200 lb. In Fig. 5, the following conditions were used: squeeze time, 20 cycles; weld time, 2 cycles; hold time, 20 cycles; net electrode force, 500 lb; transformer setting, No. 3; weld heat, 74%; shear, 2100 lb.

Although resistance seam welding is being used at the present time, the writer has not sufficient data to present definite conclusions. Tests which have been conducted to date, however, indicate that no more difficulty should be encountered in seam-welding titanium than in seam-welding Type 347 stainless steel. Flash welding is still being used in the manufacture of titanium rings. The only fusion-welding process used to date is the inert-gas-shielded metal-arc method. At Aerojet successful welds have been made both by manual and automatic methods.

Data on butt welds in 0.063-in. Ti-75A titanium sheet are given in Table 4. Tensile-test data on butt welds are given in Table 5. Data on free-bend tests on butt welds are given in Table 6.

All welds were made under actual shop conditions, using the standard equipment as manufactured today. In welding, titanium, care must be taken so that the weld is shielded at all times, particularly until it has cooled to 400° F. Since titanium has a strong affinity for oxygen and nitrogen, proper coverage by an inert gas is most important. Should helium or argon be used as the shielding gas? In tests conducted thus far, specimens welded using helium as the shielding gas have indicated a better ductility. This applies to machine welds. Argon still seems to be better for manual welding. A combination of the two gases may also be used, and a weld of higher ductility will probably be obtained than by using a straight argon in machine welding.

All the fusion welds were made using straight polarity direct current. A water-cooled torch and cup were used in making both machine and manual welds. Alternating current has not been used as yet in welding titanium at Aerojet, so that the relative merits of a c and d c are not known.

All specimens were welded on copper backup bars.

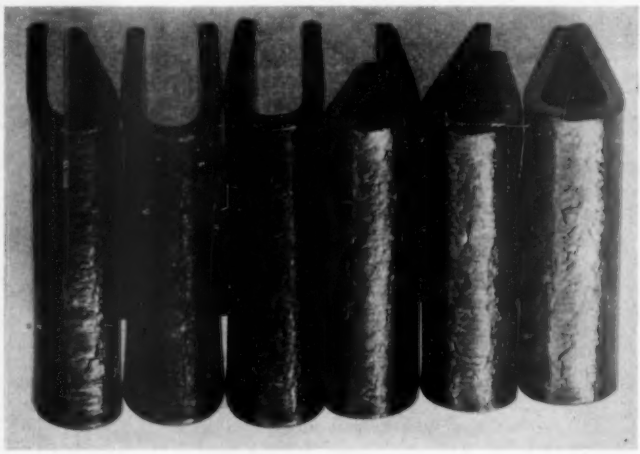

Fig. 6 Results of bending tests

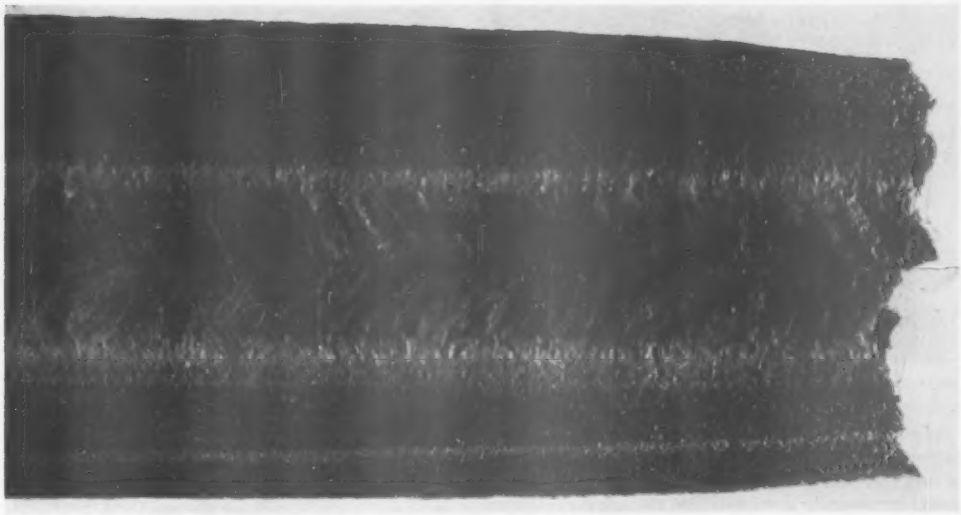

Fig. 7 Tensile specimen showing cracks in heat-affected zone

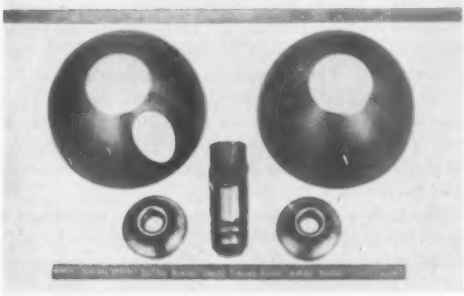

Fig. 8 Parts for small spherical tank

Table 6—180° Free-Bend Tests of Inert-Gas Tungsten-Arc Butt-Weld Joints* in 0.063-In. Ti-75A Titanium Sheet

Specimen No.	*Ratio of bend diameter to sheet thickness*	*Calculated local elongation, %*
1A	3	24
1B	3	24
1C	3	24
1D	3	24
1E	3	24
2A	3	24
2B	3	24
3A	2	30
3B	3	24
4A	3†	24
4B	4†	20
5A	12†	8
5B	14†	6
6A	1½	37
6B	2	30
7A	2	30
7B	2	30

* All-weld bead reinforcement was gound off; automatic welds were not ground. Bends are all face bends unless otherwise noted.
† Root bends.

The bars used varied from those having no groove to those having a groove ⅜ in. wide and 5/16 in. deep. In using this groove, gas was introduced which blanketed the underside of the weld. An ideal groove seems to be ¼ in. wide and 0.010 deep. At the present time it does not appear necessary to use gas on the underside when such a shallow groove is used on the backup bar.

Fig. 9 Welded titanium tank

Fig. 10 Welds around fittings

Figure 6 shows the results of bending tests. When titanium welds were rewelded over the entire length of the weld to simulate repair welding, the weld still showed surprisingly good ductility, i.e., approximately that of the original weld. However, examination of the tensile specimens showed numerous small cracks in the heat-affected zone adjacent to the weld, as illustrated in Fig. 7.

The brittleness of this zone was confirmed by subsequent bending tests, in which the specimens failed almost immediately on starting the bend. This was very noticeable on specimens that had been subjected to three or more passes, indicating that repair welding must be kept to a minimum.

A small spherical tank was fabricated for test purposes. This tank was welded manually using standard equipment. Figure 8 shows the parts before welding. All welds were carefully backed up using an inert gas. Figure 9 shows the welded tank. Figure 10 shows welds around fittings; these are manual welds. Figure 11 shows an X-ray photograph of the girth weld on the tank. The gas cup opening was $^3/_4$ in., and a $^1/_{16}$-in. tungsten electrode was used. Argon was used as the inert gas. It was noted during the fabrication of this assembly that the distortion was negligible. At the present time, the distortion of welded assemblies does not appear to be a problem.

Fig. 12 Titanium parts of 50-lb thrust rocket

The tank was hydrostatically pressure tested and a crack occurred at 2700 psi, which is equivalent to a tensile strength of 90,000 psi. The crack was repaired and the tank retested with the same results.

Figure 12 shows the parts of a 50-lb thrust rocket

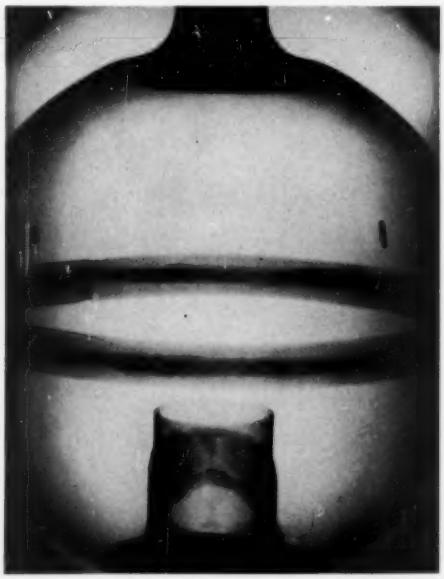

Fig. 11 X-ray photograph of tank showing girth weld

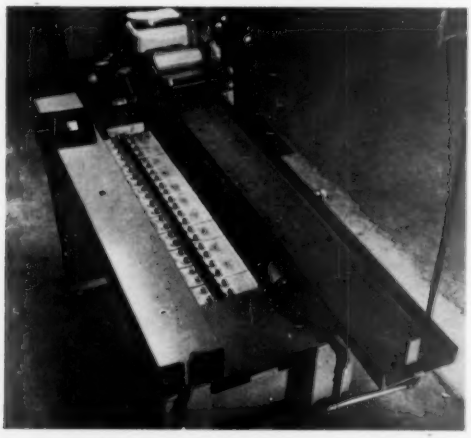

Fig. 13 Welding fixture for flat or cylindrical sections

motor prior to welding, all made of commercially pure titanium, Ti-75A.

Figure 13 shows a welding fixture used to weld flat or cylindrical sections.

The results obtained in the forming, spot welding, and fusion welding of titanium Ti-75A may be summarized as follows:

1. In general, the same forming techniques as used for stainless steel, series 300, may be used with the exception that the parts or dies should be heated to a temperature between 600 and 1000° F.

2. The same general techniques as used in the spot welding of Type 347 stainless steel may be used in the spot welding of titanium. However, a short weld cycle seems to give a much better spot weld.

3. Fusion welds can be made using standard, inert-gas-shielded arc-welding equipment. It is necessary that the weld be shielded carefully until it cools to approximately 400° F.

4. Welding procedures and design must be worked out so that the number of welds coming together at any one point is kept to a minimum. At the present time, results indicate that a weld should not be gone over more than one time.

5. As with any fusion welding, good tooling is essential.

The writer believes that welding of titanium alloys, Type 75A, can be accomplished on a production basis provided that the points mentioned are followed.

ACKNOWLEDGMENT

The writer wishes to express his appreciation for the help given by E. W. Brady, Group Leader, Metallurgical Group, Aerojet Engineering Corp.

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(*Continued from page 1016*)

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Strength Joints to Steel with Aluminum Bronze Filler Metals

Brazing and welding concepts for joining dissimilar metals with particular reference to aluminum bronze filler metals

by Willis G. Groth and Robert E. Maersch

INTRODUCTION

PRESENT-DAY material prices and availability brought new attention to the possibilities of composite fabrication utilizing critical or expensive metals for only those components absolutely requiring their specific unique qualities. Many structures, vessels and machines used in the process industries utilize certain corrosion-resistant alloys for critical parts such as tube sheets, shells, piping, etc. Other items such as flanges, legs, clips, (and even shells exposed to noncorrosive media) will normally be flange quality steel plate.

The welding of the common industrial metals and alloys has received a great deal of attention for many years and procedures can be obtained in most welding handbooks and manuals. One matter which has received little technical attention is that of joining dissimilar metals. This type of weld eliminates the possibility of using filler metal of "like" composition, since the filler metal would necessarily not be similar to both base metals. In this case it is customary to specify a filler metal whose composition approximates that of one side of the joint desired, and to try to bond or weld to the other or "dissimilar" material. There are many uncharted corridors in our metallurgical technology structure, and in the matter of welding dissimilar metals by a fusion technique the welding engineer finds himself stepping into utter darkness. For this reason he often hesitates to step at all.

THE BRAZING CONCEPT

How do we proceed if we are to realize the full advantages of our high-strength copper base alloys in *composite* structures? We *know* that aluminum bronze-welding electrodes and filler rods have been used successfully for many years for such fabrications, but the exact welding procedure or desirable metallographic structure is not always fully understood. This paper concerns itself with the strength joint likely to be found in composite vessel fabrications, and the metallurgical conditions and explanations may of course not apply equally well to aluminum bronze joints to cast iron, high carbon and alloy steels, or the new alloy cast irons.

Welding engineers are aware that steel and certain copper base alloys are not exactly incompatible where welding is concerned. For this reason copper base alloy filler metals have been used for many years for the repair of cast iron and steel machine parts where color match was unnecessary. This repair was usually performed, as it is today, by utilizing a braze bond which required that the copper base alloy wet the base metal but not necessarily melt it or alloy with it. The original concept may have been based on experience which pointed out the advisability of braze welding when joining to steel with phos copper alloys and silicon copper alloys. In any event, this is the concept that has carried over into published recommendations for "braze welding" of steel with aluminum bronze filler metal. It is this recommendation, with the various explanations offered to justify it, which the writers find to be ill-founded and valueless where *strength* rather than *overlay* deposits is concerned.

THE WELDING CONCEPT

The melting of mild steel and its mixture with aluminum bronze filler metal will not produce a "brittle" or "weak" weld deposit, fusion zone or heat-affected zone although many magazine articles and even books may leave that impression. There are, however, certain aluminum bronze alloy proportions which will give optimum combinations of mechanical, physical and corrosion-resistant properties. There has been much material published on the advantages of adding

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Table 1—Filler Metal Specifications Based on Analysis of Rod or Electrode Before Welding

	Cu	*Al*	*Fe*	*Zn, max*	*Si, max*	*Pb, max*	*Total, others*
R-CuAl-A1*	Rem.	6.0–9.0	Not specified	0.02	0.10	0.02	0.50
E-CuAl-A1†	Rem.	6.0–9.0	Not specified	0.20	0.10	0.02	0.50
R-CuAl-A2*	Rem.	9.0–11.0	1.5 max	0.02	0.10	0.02	0.50
E-CuAl-A2†	Rem.	9.0–11.0	1.5 max	0.02	0.10	0.02	0.50
R-CuAl-B*	Rem.	11.0–12	3.0–4.25	0.02	0.10	0.02	0.50
E-CuAl-B†	Rem.	11.0–12	3.0–4.25	0.02	0.10	0.02	0.50

Specifications of ASTM B169–51T alloy D aluminum bronze plate, sheet, strip, etc.

Cu, %.....88–92.5
Al, %.....6–8
Fe, %.....1.5–3.5
Cu + Al + Fe.....99.5%

* AWS A5.7–52T.
† Specification pending.

iron to an aluminum bronze alloy to increase its tensile strength, yield strength, elongation, toughness, etc. It *is* true that an iron pickup which has alloyed with the aluminum bronze weld deposit will usually increase the yield strength, tensile strength and hardness of the deposit with a moderate loss in ductility.

The term "brittle" is not usually applied to materials having an elongation in excess of 5%, and for this reason the term should not be applied to welds made in ferrous material with aluminum bronze filler metal of the AWS-ASTM E CuAl-A2, R CuAl-A2, E CuAl-B or R CuAl-B specifications. See Table 1.

Failures to pass regulatory code requirements with dissimilar metal welds to steel made with aluminum bronze filler metals have usually been encountered in the *bend* tests. The writers know of no fabricator, required to pass ASME Boiler Code or AMERICAN WELDING SOCIETY standard welding tests between steel and the high-strength copper base alloys, who has been able to successfully join such materials to obtain the full required strength and ductility of the steel in bending and shear without melting some steel into the filler metal at the interface. It was the question as to the condition of this interface between steel and aluminum bronze or the desirability of a "fusion line" as against a "fusion zone" which initiated the work on this paper.

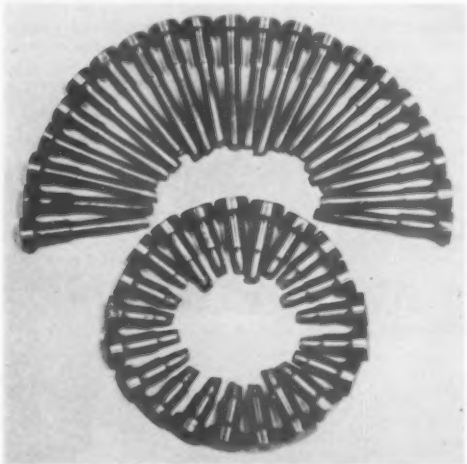


Fig. 1 0.505 transverse tensile specimens, aluminum bronze welds in flange quality steel plate

TENSILE TESTS IN MILD STEELS

Figure 1 shows a number of 0.505-in. transverse tensile specimens taken from test welds in flange quality mild steel plate. Some of the welds were made by the metal arc process using AWS-ASTM E CuAl-A2 electrodes and the remainder were made by using the carbon arc process and AWS-ASTM R CuAl-B filler rods. It is obvious that from *strength* standpoint the welds were above criticism since they fractured in, and developed the full tensile strength of, the steel base metal.

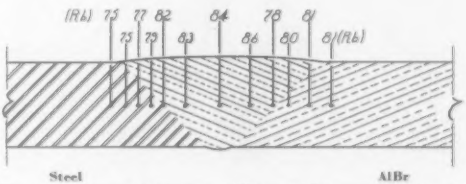


Fig. 2 Hardness Traverse. AlBr weld between mild steel and aluminum bronze, as-welded

HARDNESS TESTS IN MILD STEEL

Many hardness and ductility tests have been made to determine if there exists in such a joint a zone of limited strength or ductility. The accompanying photographs and photomicrographs are a few of those secured in this investigation and these reveal graphically the reasons for the writers' recommendations that such welds should involve a definite and controlled but absolutely continuous fusion of the base metal with the weld metal if optimum joint properties are to be secured.

Figure 2 shows the cross section of a butt joint between $^3/_8$-in. mild steel and ASTM B169–51T - alloy D aluminum bronze plate showing the Rockwell B hardnesses of the weld, base metal and fusion zone of a *fusion* weld between these materials using AWS-ASTM E CuAl-A2 filler metal.

Specifications of ASTM B169–51T - Alloy D Alu-

minum Bronze Plate, Sheet, Strip, Etc.: Cu, 88–92.5%; Al, 6–8%; Fe, 1.5–3.5%; Cu and Al and Fe = 99.5% min.

BRAZED JOINT TEST SPECIMENS AND DATA

After examining a great many tensile and bend specimens and then making new specimens to duplicate fusion line conditions the writers have concluded that a fusion line shear failure is almost certain to occur in either bend or tensile tests if the base metal is not melted into the deposit to form a fusion zone rather than a fusion line. The writers have made and secured many joints which upon failure revealed the original tool marks or grinder marks in the base plate, covered by only a very thin film of bronze to give the shear fracture surface a golden appearance. See Figs. 3 and 4.

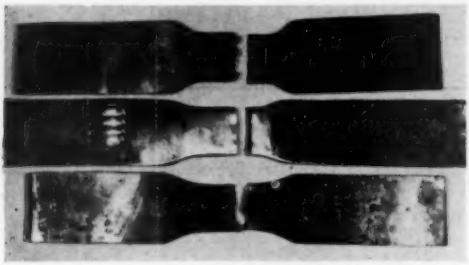


Fig. 3 Transverse tensile specimens showing partial or complete shear failure of "brazed" joints

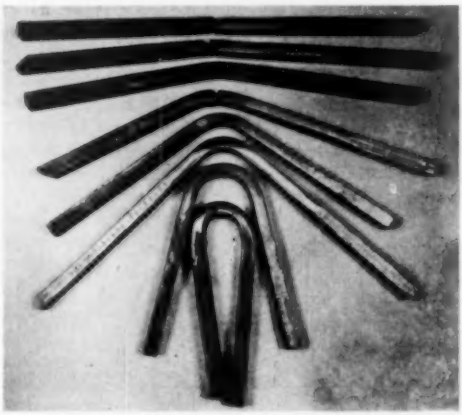


Fig. 4 Face and root bend specimens showing unreliability of "brazed" joints in bending

The explanation seems to be that a true brazed joint without alloying places all grain boundaries in one continuous plane so that a slip plane need cross no irregular grain or crystal boundaries. The converse of this, with dendrites of steel or iron rich material going into the aluminum bronze deposit, appears to be the strongest joint available and will produce the strength of the weaker metal upon testing. Figures 5 and 6 show a braze type bond and a fusion type bond taken from, respectively, the face pass and the root pass of an AWS-ASTM E CuAl-A2 aluminum bronze butt weld deposit against mild steel (1000 ×).

Table 2—Fillet Weld Shear Strength

Identification	*Type of bond*	*Max. load, lb.*	*Total length, in.*	*Average theoretical, throat, in.*	*Shear strength, psi*
627B	Weld	53,600	6,000	0.156	53,200
628C	Weld	52,000	5.987	0.212	48,400
627D	Braze	32,400	6.000	0.188	31,400
628D	Braze	35,300	5.812	0.125	38,800

NOTE: Specimens B and C failed in shear through the weld throat. Specimens D sheared along the weld interface for some distance, revealing a total lack of actual penetration or fusion to the steel and therefore a relatively low shear strength along that "brazed" interface. Braze welding had been attempted in all cases, and all welds were made by the same operator.

An example of relative shear strengths of such "brazed" and "welded" joints will be found in Table 2 (aluminum bronze to mild steel, aluminum bronze

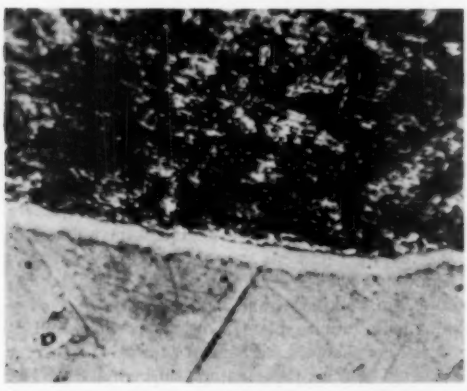


Fig. 5 "Brazed" bond between mild steel and aluminum bronze. 1000 ×

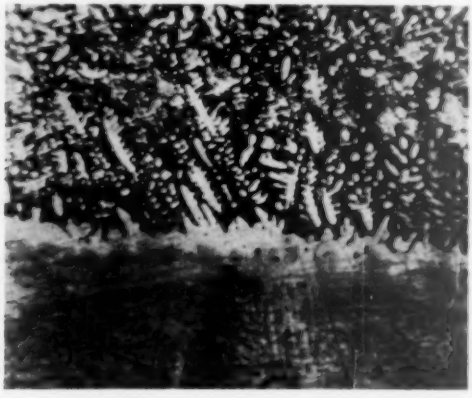


Fig. 6 "Welded" bond between mild steel and aluminum bronze. 1000 ×

fillet welds); AWS longitudinal shear specimens, AWS-ASTM R CuAl-B and E CuAl-B welds.

THE FUSION ZONE AND TEST REQUIREMENTS

Since it is necessary that the fusion line be broken up by melting of the steel base metal and active interlocking of grains along the fusion line where the deposit solidifies, it is obvious that this is best accomplished by a welding process which permits a high degree of operator control of penetration. There is no necessity for diffusion for any considerable distance beyond this interface, and excessive diffusion and alloying will only increase the tensile and yield strength of the weld deposit. A weld which has dissolved or alloyed the ferrous material and therefore increased its tensile and yield strength is less desirable than a weld without such alloying beyond the interface since the aluminum bronze deposit is already somewhat harder or stronger than the mild steel base material.

Since we have mentioned the high strength of this deposit, we must return to the original statement "it is customary to specify a filler metal whose composition approximates that of one side of the joint desired." This approach to the problem eliminates welding difficulties with one side of the joint, but it does not necessarily take into consideration mechanical properties of either side of the joint. For this reason, most welding codes and standards require that such a joint develop mechanical properties equal to the lower value specified for the material on either side of the joint. That is, the joint must develop the lower tensile strength, the lower yield strength if yield strength is specified, and the lower ductility if ductility is specified. Where the yield strength ranges of the base metals are similar and a weld deposit of comparable properties can be placed one should experience little difficulty in meeting face, root and side bend requirements. The bend may, however, try to concentrate in the softer or lower yield point material along the fusion line of the weld when joining a high-strength bronze to mild steels. This concentrates the stress at what is presumed to be the least reliable part of the joint in this type of a welding application. For that reason difficulty may occasionally be experienced in making satisfactory transverse bend tests of such joint specimens. (It is, however, unusual to find dissimilar base metals which when welded together will bend smoothly and uniformly at and adjacent to the joint in a guided-bend fixture.) In the usual pressure vessel application one is required to weld steel of approximately 30,000 psi yield strength and approximately 60,000 psi tensile strength by the use of a filler metal of approximately 35,000 to 40,000 lb yield strength and approximately 80,000 to 100,000 lb tensile strength (after dilution with the base metal).

Several authorities on the subject have suggested that the industry is perhaps being too exacting in its requirements for such welded joints since this type of joint is rarely subjected to extreme bending loads. Since it is obvious that the engineer must endeavor to find a material of mechanical as well as chemical compatibility he rarely finds AWS-ASTM E CuAl-B or R CuAl-B filler metals used in joints to mild steel where *bend tests* must be made to satisfy code requirements for pressure vessels, since the yield and tensile strengths of such deposits would be unnecessarily high. AWS-ASTM E CuAl-A2 or R CuAl-A2 filler metal is, however, a very popular material for dissimilar metal joints to steel for pressure vessel applications such as welds between steel shells and nonferrous tube sheets or between steel flanges and nonferrous piping.

EFFECTS OF HEAT TREATMENT

Metallurgically, the high iron aluminum bronze deposit of AWS-ASTM E CuAl-A2, R CuAl-A2, R CuAl-B or E CuAl-B analysis, deposited on steel, is usually a duplex alloy. For that reason it is susceptible to heat treatment, and the heat-treat cycles may resemble those for medium carbon steel. While the "hardenability" does not nearly approach that of steel, a substantial change in mechanical properties will be observed upon quenching from normal carbon steel heat-treat temperatures. Table 3 charts typical hardness changes upon heat treatment of AWS-ASTM E CuAl-A1 and E CuAl-A2 deposits on mild steel. This table takes into consideration the distance from the interface and consequently the iron dilution from the base metal.

Table 3—Hardness Changes

Rockwell "B" readings

From interface, in.	*As-welded, Air cool*	*1150° F Air cool*	*1150° F water quench*
AWS-ASTM E CuAl-A2			
0.03125	87	80	88
+1/16	85	82	88
+1/8	87	84	89
+3/16	86	83	89
AWS-ASTM E CuAl-A1			
0.03125	80	77	80
+1/16	73	69	65
+1/8	57	43	58
+3/16	46	40	41

As is characteristic of other materials, hardness variations in a given aluminum bronze alloy will in general be accompanied by ductility variations. As would be expected, the ductility or elongation decreases as the hardness increases. For this reason the tabulated information in Table 3 would indicate the general value of the stress-relief heat treatment for steel composite pressure vessel fabrications. It has been found that a conventional steel vessel heat treatment of 1150° F for 1 hr followed by an air cool will quite often improve the general composite joint ductility. Where high ductility is an absolute requirement as in meeting face and root bend requirements for Code or Underwriters' approval, and the weldment can be so stress relieved, it may be advisable to so specify. In a good many fabrications it would not, of course, be justified.

OTHER METALLURGICAL CONSIDERATIONS

Since we have been recommending the use of *fusion welding* in the making of strength joints to steel with aluminum bronze filler metals and have endeavored to explain the metallurgical effects of alloying with the base metal, we must in all fairness extend our discussion to include those welds containing unalloyed inclusions of mild steel.

The net effect of included iron or steel in an aluminum bronze alloy or weld deposit depends upon several factors. There may be a certain amount of included ferrous material which has had time and opportunity to alloy with the aluminum bronze. There may be other ferrous material which is merely contained as mechanical inclusions. When it was first proved that a fusion type weld interface was necessary for optimum joint quality, steps were taken to determine just how much penetration and weld metal dilution was advisable. These tests revealed that the ideal fusion line would show an intermingling of the aluminum bronze weld metal and the steel base plate at the interface. It was further determined as noted previously that a "fusion zone" of less than $^1/_{16}$-in. thickness was fully as satisfactory as a "fusion zone" of greater depth.

Insufficient ductility was found to exist where the iron pickup or dilution had been abnormally high (over 20%) throughout the weld deposit and much of this iron existed as spheroids or irregular globules suspended in the aluminum bronze deposit. Figure 7 (78 ×) shows small steel spheroids in an aluminum bronze deposit such as was shown in Figs. 5 and 6, and these globules may in some cases be relatively large.

THE WELDING PROCEDURE

Now that the metallographic and physical characteristics of brazed and fused interfaces have been described, the reader might logically ask "how can an operator assure himself that he is doing a good job?" The writers have made many welds possessing the desirable characteristics outlined above. Others have been made by an operator in our own plant.

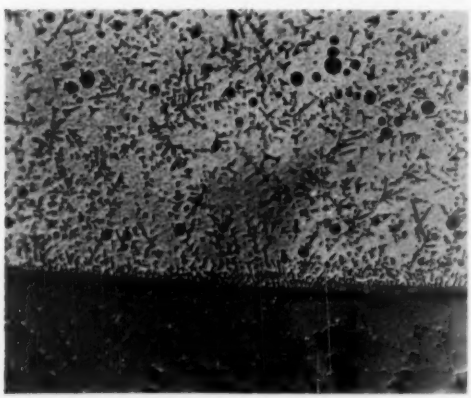


Fig. 7 A "fusion weld" interface with globular inclusions of mild steel entrapped in the aluminum bronze weld deposit

Our tests have revealed the practicability of fusion welding to secure consistently high quality joints, and the welding procedures required are easily within the capabilities and understanding of operators qualified to work in other materials.

The writers have stressed the desirability of securing an absolutely continuous *fusion zone* of minimum depth along the *steel* interface of the joint. They have also pointed out the desirability of using a welding process which will afford a maximum degree of operator control of the weld deposit. The "filler rod" processes (carbon-arc and inert-gas tungsten-arc) provide a high degree of control since filler metal is added only when the operator considers his conditions to be "right."

The inert-gas consumable-electrode process provides a concentrated arc, close control of arc length, heat and bead cross section. It also provides *continuous* beads to avoid possible inconsistencies resulting from breaking the arc.

Most of the field work done to date has been performed with the metal arc process. This process accounts for a large portion of the aluminum bronze weld metal used in fabrication and repair work requiring the joining of steel to copper base alloys. The process is fast, economical, requires no special equipment and is the preference of many fabricators for use where the very highest radiographic and other standards of quality are not required.

Joint design considerations will normally require a groove-included angle of 90° for carbon arc and metal arc welding. An angle of 75° is desirable for the inert-gas welding processes, with angles to 60° being satisfactory in most instances.

The edge preparation of the *steel* side of the joint should provide a $^1/_{16}$-in. land or nose. Heavier lands are hard to fuse down and sharp lands result in excessive dilution. The joint should be spaced at least $^1/_8$-in. so that complete penetration can be secured without excessive melting of the base plates in the first or root pass.

A stringer-bead procedure must be used against the *steel* base material to provide continuous and uniform fusion. Special pains must be taken with the inert-gas-shielded welding processes to secure a uniform penetration since the excellent wetting of aluminum bronze on steel by these processes tempts the operator to flow on large beads which merely wet the surface. Figure 8 shows a typical pass procedure for a weld between two $^3/_8$-in. steel plates. Note that the stringer beads

Fig. 8 Bead sequences and sections, AlBr weld in $^3/_8$-in. weld steel

have been so interlocked as to provide a reasonably uniform depth of fusion. Note also that the welding procedure on subsequent passes is not critical.

Current ranges are not unusual or critical, as long as the first stringer beads are deposited at a travel rate which permits the arc to fuse the base plate. The carbon-arc and inert-gas tungsten-arc [a-c with argon, or d-c straight (negative) polarity with helium] processes will require amperages of approximately 200–250 for placing the first beads in the root and on the surface of the steel base plate. The inert-gas consumable-electrode process with $^1/_{16}$-in. diameter wire can place very satisfactory stringer beads at amperages between 250 and 275. In metal arc welding these passes are normally placed with $^5/_{32}$-in. electrodes at amperages of approximately 160–180.

SUPPLEMENTARY DATA USING OTHER STEELS AND TEST SPECIMENS

In the routine investigations referred to above it was proved that an aluminum bronze strength weld, placed according to recommended procedures, was stronger than the flange quality steel plate base metal. The question quickly arose "just how strong *is* the deposit?"

Aluminum bronze has been used commercially for the joining of hardenable and difficult-to-weld steels. It is difficult and costly, however, to machine test bars from welds made in fully hardened steel. Since the hardenability of medium carbon steel far exceeds that of an aluminum bronze weld deposit, the writers decided to weld such a steel in the annealed condition, machine the test specimens and then heat treat the specimens to try to develop in the steel approximately the same tensile strength found in the aluminum bronze weld metal.

Several butt joints were made in 1-in. medium carbon steel plate using the inert-gas consumable-electrode process and AWS-ASTM E CuAl-A2 filler metal. High current densities were used (over 300 amp) and an unnecessarily high general penetration and weld metal dilution resulted. Deposit analysis was as follows: Cu, 77.5%; Al, 9.65%; Fe, 12.60%; Ni, 0.22%; Mn, 0.03%.

Notwithstanding this high iron content, the all-weld-metal specimens developed a tensile strength of 100,000 psi and an elongation of 18% in the as-welded condition. This tensile strength is 10,000–15,000 psi greater than would be expected in welding an aluminum bronze of analysis similar to that of the weld rod being used. The elongation is some 10% less than would be expected in similar circumstances.

Figure 9 shows three bars removed as side bends from the above weld. They were bent in a free bend manner to show up the difference in deposit and base metal yield strength. Bar 1 (left side) was bent as welded, bar 2 (center) was bent after an 1150° F stress-relief cycle, and bar 3 (right side) was oil quenched from 1450° F. The bend bars shown were *not* bent to failure,

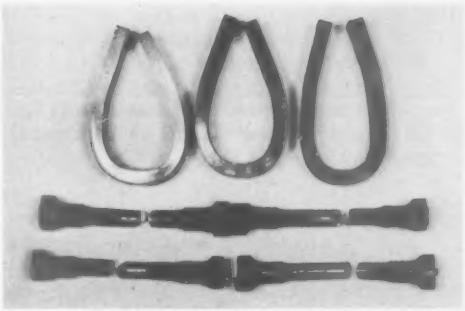


Fig. 9 Free side bend and transverse tensile specimens

but merely bent until the ends touched, and the space between those ends represents "spring back" of these specimens. While the weld deposit in bar 3 appears to have been the softer since it assumed a smaller bend radius, this can be attributed to an increase in the strength of the steel-plate rather than a decrease in the strength of the weld deposit. Figure 10 shows two side bend specimens from an identical weld, but bent in the standard bend jig. Bar number 1 (left side) was stress relieved for 1 hr at 1150° F and air cooled. Bar 2 (right side) was water quenched from 1500° F and given a 400° F draw. The steel in bar 1 had a Brinell hardness of 112, that in bar 2 a Brinell of 169. Again the bars bent satisfactorily to demonstrate the strength and ductility of an AWS-ASTM E CuAl-A2 fusion weld to steel.

Figure 9 also shows several transverse weld-reduced section tensile specimens taken from the same weld. These specimens were water quenched from 1450° F after machining since it was found that the less drastic treatments noted above left the strength of the steel somewhat below the strength of the weld deposit. These tensile bars failed at from 90,000 to 94,500 psi tensile strength. While some of the failures did occur in the region of the fusion line, the fractured surface shows substantial amounts of steel welded to the aluminum bronze interface. These heat-treated medium carbon steel specimens were used to provide a higher

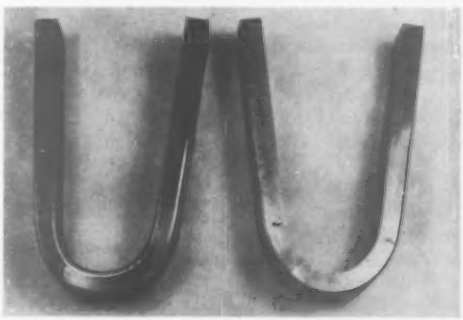


Fig. 10 Conventional side bends

strength steel base metal so as to more nearly approach the strength of the aluminum bronze deposit and find the actual strength of such a deposit.

The writers feel that the reader may be interested in some of the more unorthodox tests made during this investigation and with that in mind we are including photographs in Figs. 11 and 12.

Fig. 11 Free-bend bar twisted to show deposit ductility

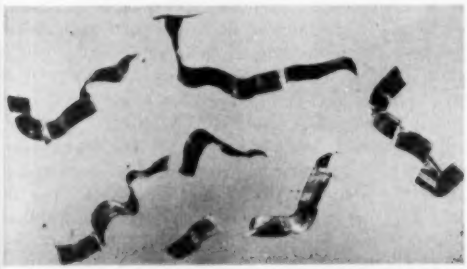


Fig. 12 Lathe turnings including both steel and aluminum bronze sections from welded joint

Figure 11 shows a free-bend specimen removed from a butt joint between 1020 hot-rolled steel and ASTM B169–51T alloy D specification aluminum bronze plate in $^3/_8$ in. thickness. The weld was deposited by the inert-gas consumable-electrode process using AWS-ASTM E CuAl-A2 filler metal. Since the face and root bend specimens met specifications satisfactorily the writers decided to subject several free-bend bars to a twist test in an attempt to show more graphically the extreme ductility of the welds and the fusion and heat-affected zones. While these specimens were not twisted to failure the photograph shows that the joint is quite reliable. It also reveals the differences in yield strength of the base metals being used. Figure 12 above shows an assortment of lathe chips secured when machining 0.505-in. tensile specimens from butt joints in 1-in steel plate. This makes a particularly striking display in color since the steel sections of the chips turned blue during the turning operation to outline the steel portions and interfaces.

CONCLUSION

1. Aluminum bronze filler metal can be used to secure code-quality welds between steel and the high-strength copper base alloys.
2. Such welds must be made by a fusion technique rather than a "brazing" technique.
3. These welds can be made by any of the welding processes commonly used for joining aluminum bronze.
4. The techniques and procedures are not unique and therefore these welds can be duplicated by operators qualified for code quality work in other materials.

ACKNOWLEDGMENT

The authors wish to express their thanks to the following men for their assistance and counsel in the preparation of this paper: W. E. Claflin, H. H. Hose, V. C. Abaravich and W. H. Gruss.

Hear — The latest news on engineering development from the industry's foremost authorities

Marvel — At Houston's welded office, hospital & factory buildings; more than in any equivalent area in the world

Plan — To attend the all welding event at the famed Shamrock Hotel and connecting Hall of Exhibits—both fully air conditioned.

Stud Welding Saves Time

STUD welding, the war-born end-welding technique that saved Canadian and United States ship builders an estimated 100 million man-hours during World War II, is helping Canadian Car and Foundry Co. and other car builders make comparable time and cost savings in the construction of rolling stock, particularly boxcars, automobile cars and refrigerator cars.

At Canadian Car's Dominion Shops in Montreal, for instance, two men armed with lightweight Nelson stud welding guns now find it an easy task to install the furring strips and retainer studs used to fasten wood liners to the car ends for the day's run of cars—a job that used to keep three hand welders much busier all the time. While a crew of four join the upper and lower halves of each car end by seam welding in a special jig at the preceding station, one of the stud welders installs five short ($^1/_2$- x $1^1/_4$-in.) retainer studs along the top and about half of the 24 furring studs. The second stud welder installs the balance, after first completing work on the hand-welded seams which cannot be done in the jig.

Fig. 1 Operator installs four granular flux-filled studs on each of four corner posts and each of the four door posts on each car

He uses simple wooden templates which insure accurate positioning, and finds it an easy task to keep up with his quota. Because the studs used on corner posts and door posts are of different lengths, two different fixture settings are required. The management has provided two guns, which are operated from a single timer and welding generator, so that there is no need to change back and forth

Fig. 2 Three stud-welding guns equipped with fixtures to handle studs of different lengths are used by these two operators to install the five retainer studs and 24 furring studs on each of the ends which pass across this station

Marcel Genert (standing) finishes the seam welding job on the upturned edges at this station before he joins Charles Poirier (right) who installs the five $^1/_2$- x $1^1/_4$-in. retainer studs in the extreme background and better than half of the 2-, $2^1/_2$- and $3^1/_2$-in. studs that are welded in the valleys of the corrugations.

This procedure enables two men to set their own pace on a job which used to keep three hand welders busy all the time.

In addition to the man-power saving and decreased pressure at this station, J. Grant Mitchell, chief engineer and assistant production manager, points out that use of stud welding on corner posts and door posts has enabled one man to do work that previously required a crew of three. A single stud welding operator, aided only by two simple wooden templates and two stud welding guns preset for two different lengths, keeps up with the daily production schedule, handling all the work and installing 896 threaded studs on a single shift.

The precise timing and accurate positioning of studs installed by this semiautomatic welding process greatly expedites the installation of predrilled furring strips and prevents corrosion and leakage around the door posts, where through-bolts with rivet heads were previously used. Ed Merrill, general superintendent, Turcot Works Canadian Car, in commenting on the

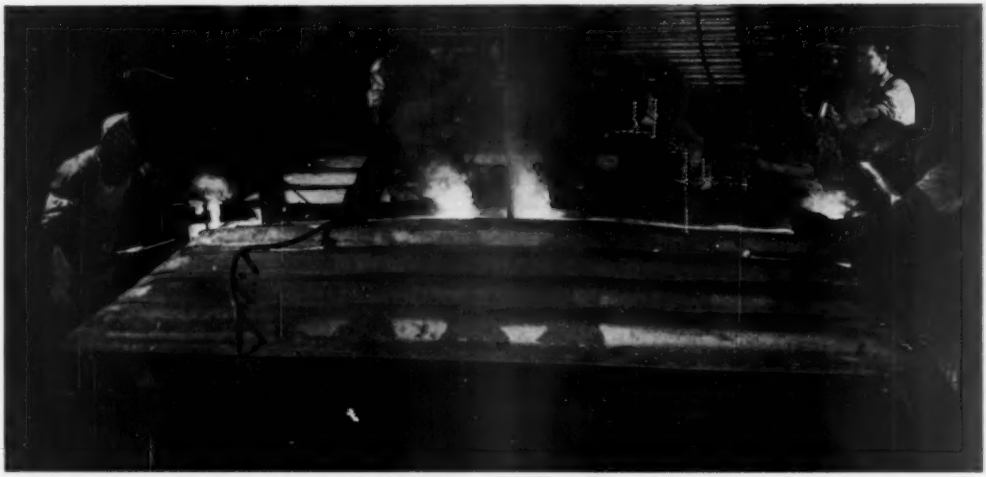


Fig. 3 After four men at Canadian Car and Foundry Co.'s Dominion Shops hand-weld the seam which joins the top and bottom halves of each car end in a special jig (shown here), a jib crane transfers the assembled end to a welding bed in the background, where two stud welders (crouched) are installing furring studs with the Nelson stud-welding gun

stud welding process, cited the fact that his operators, on a recent run of 900 cars, installed more than 80,000 granular flux-filled studs with practically perfect welding results.

Unionmelt Cladding of Mild Steel Roll by Series-Arc Technique

by J. F. Collins, H. I. Shrubsall and J. L. Wilson

CONSIDERABLE research work has established the value of the series-arc technique of Unionmelt welding for the surfacing of mild steel with various types of alloys. This report is of interest not only as it indicates the progress in the application of the series-arc technique, but also as it shows the ease with which it was used.

A steam-jacketed roll used for drying paper was successfully clad with a deposit approximating AISI Type 501 metal. Welding this type of roll usually presents difficulty due to warpage and shrinkage. Although longitudinal shrinkage occurred, cladding was accomplished in a straight-forward manner after one or two initial passes had been made to establish welding conditions.

The roll was 24 in. long and had a diameter of 23 $^{13}/_{16}$ in. The outside shell was $^{3}/_{4}$ in. thick. This was fastened to the inside shell by end plates approximately 1 $^{1}/_{4}$ in. thick, and wide enough radially to provide a space of about 2 in. between the outside and inside shell. In operation, steam is admitted to this space to heat the roll. The roll is Type A-285 mild steel having the following analysis by percent: C, 0.30; M, 0.80; S, 0.05; P, 0.04.

Welding was done by the series-arc technique using two Unionmelt Type D heads. "The welding electrodes were positioned in a vertical plane parallel to the longitudinal axis of the cylinder. Each rod was positioned at 22 $^{1}/_{2}$ deg from the vertical (45 deg between rods). A spacing of $^{1}/_{4}$ in. between the point of intersection of the electrodes and the roll surface was found to give best results. The electrode intersection was located $1^{9}/_{16}$ in. off-center of the top of the cylinder

J. F. Collins, H. I. Shrubsall and J. L. Wilson are Research Engineers with the Union Carbide & Carbon Research Laboratories, Niagara Falls, N. Y.

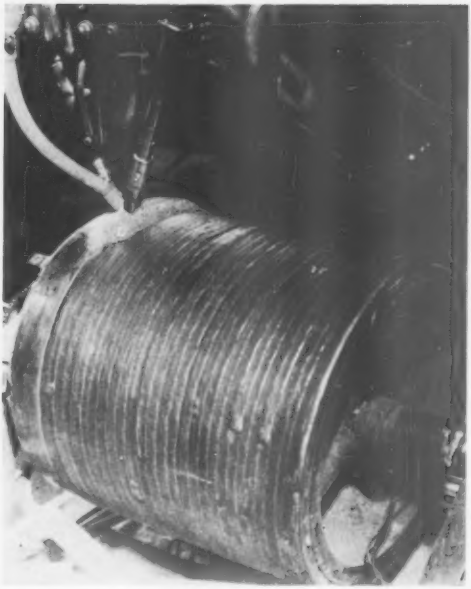


Fig. 1. Smooth, overlapped bands of Type 501 metal are deposited by series-arc welding on a mild steel roll

in the direction opposite to rotation. Figure 1 shows the apparatus in operation.

Welding conditions were 400- to 425-amp a c, 20–25 v for each head and welding speed about 10 ipm. The deposit was applied as a number of individual overlapping welds, rather than in the form of a helix. For making the overlay, a $^{3}/_{16}$ in. diam rod of medium-carbon manganese silicon, chromium, molybdenum alloy specially designed for Unionmelt welding, and known as Oxweld No. 2842 was used.

Unionmelt welding composition used was Grade 70, size 12 x 200. During the welding operation the roll was held in an especially designed jig and was driven by a sprocket and chain attached to a variable speed rotating table.

A $^{1}/_{4}$- x 1 $^{1}/_{2}$-in. copper strip was fastened to the edge of the cylinder to act as a chill bar, thereby permitting the two end weld deposits to be made flush with the edges of the cylinder. Throughout the welding procedure, the temperature was not allowed to go below 100° F nor above 600° F. The cylinder shrank longitudinally, as would be expected, about $^{3}/_{8}$ in. per foot, as a result of the repeated heating and cooling of the shell during welding. Approximately 88 lb of welding electrode were used to make a deposit about $^{3}/_{16}$ in. thick over the entire surface. In thin-walled shells of this type allowance should be made for some degree of longitudinal shrinkage. In heavy-walled cylinders, the shrinkage would be very small.

The chromium content of the overlay, determined from a sample machined from the roll, was 4.26%. A 6-in. wide strip of deposited metal was machined from the roll to obtain a comparison between the as-welded and the machined surface. Figure 2 shows at the right the actual surface overlay as-deposited. Note that each successive deposit from left to right overlaps the previous one by about one-third its width. The overlay is smooth and sound. After machining, the metal coat on the roll was approximately $^{1}/_{8}$ in. thick. At the left, the machined surface is seen to be free from defects. The lighter colored markings at the junction of the successive deposits result from heat effects on the grain structure but the machining is uniform.

The series-arc technique is particularly suited to this type of work. Resulting deposits are uniform in thickness and free from porosity.

Fig. 2. Comparison of the as-welded and the machined surfaces of the roll. The deposit is made with Oxweld No. 2842 (a modified Type 501) rod

A New Welded Railway Bridge in Switzerland

by M. Frei

THE wrought iron bridge (called the Tannwald Bridge) built in 1854 across the Aar River at Olten is being replaced by a heavier plate girder bridge, half of which has been put in service (Fig. 1). The piling of the old bridge has been adapted to the new (Fig. 2).

It was realized in designing the new bridge that the selection of a steel for a welded bridge is more important than for a riveted bridge. In view of cracks that occurred in a few welded structures, it is essential to use a steel having a high capacity for plastic work after welding and at low temperatures. The builders of the bridge, Wartmann & Co., consulted with Mr. Schnadt and decided to use two steels: Z_1 and Z_2. Steel Z_1 is used for the most highly stressed portions of the bridge (Fig. 3). It is a normalized aluminum-killed basic Bessemer steel with 0.13% carbon max, 0.05% S max, 0.05% P maximum. It has good Schnadt impact value at +5° F even after aging. For the less highly stressed parts, or for parts under compressive stress, steel Z_2 was used. It has

Abstract of "Un nouveau pont de chemin de fer en acier soudé," published in *L'Ossature Métallique*, 17, 249–255 (May 1952). (Abstracted by Dr. G. E. Claussen.)

Fig. 1 The new welded railway bridge

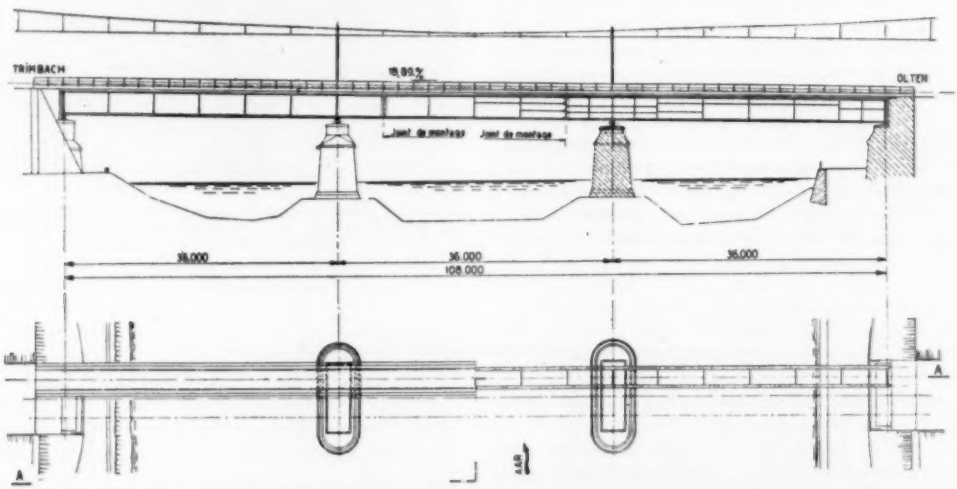


Fig. 2 Elevation and plan of the new bridge, showing the two field-welded joints. 36.00 meters = 117 feet

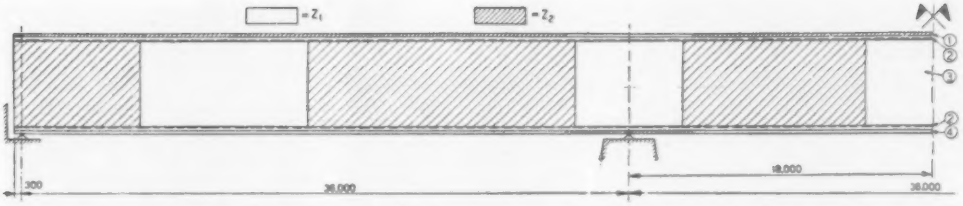


Fig. 3 Distribution of Steels Z_1 and Z_2. ①, Compression flange; ②, angle; ③, web; ④, tension flange

the same chemical limits as Z_1, is aluminum-killed, but is not normalized. Unkilled steel was avoided because of segregations of S and P which affect the brittleness after welding.

Welding was performed with Univers W electrodes made by Oerlikon. They have a basic (low-hydrogen) coating depositing weld metal that remains tough at 5° F and after aging.

The design also was arranged to secure maximum ductility in three ways: (1) Avoid variations in cross section and surface irregularities; (2) lower multiaxial stresses by staggering certain welds, reducing weld volume to a minimum and maintaining a strict sequence of welds; (3) place relieving holes at locations having high tensile stresses. Thus the shrinkage stresses are distributed more uniformly.

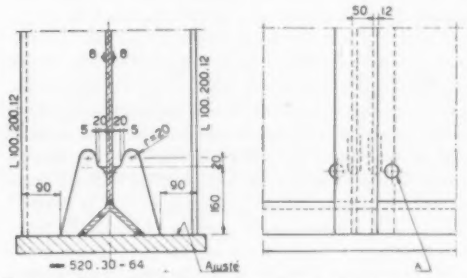


Fig. 4 Section through tension flange showing holes A to decrease rigidity. Dimensions in millimeters

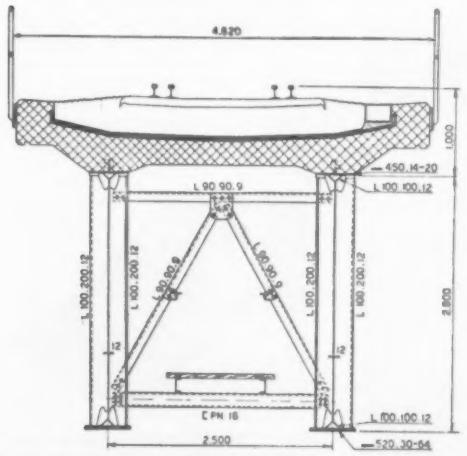


Fig. 5 Transverse section at mid-span. Dimensions in millimeters

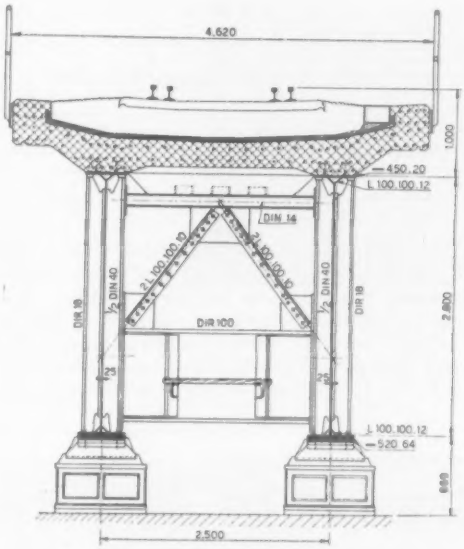


Fig. 6 Transverse section at the supports

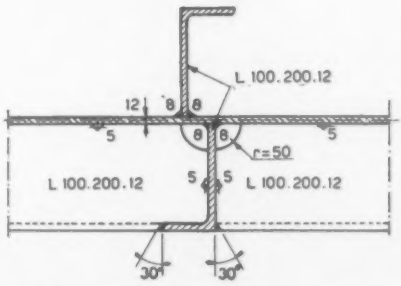
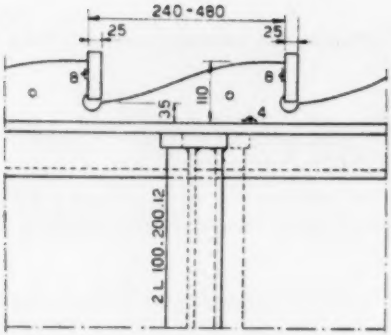
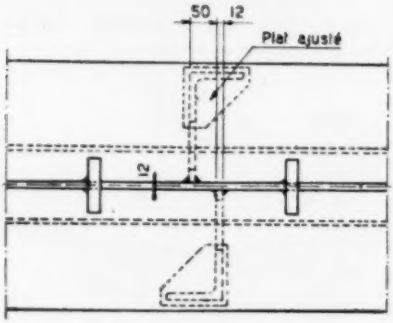


Fig. 7 Detail of stiffeners showing sniping





The web-flange connection consisted of an angle (Fig. 4) which avoided the use of nose profile flanges, experience with which has been unsatisfactory. The angle connection is a patent of the Dörnen firm in Dortmund, Germany, where it has been used frequently. Tests showed that the angle-to-flange welds had a yield value longitudinally of 30,000–34,000 pounds per

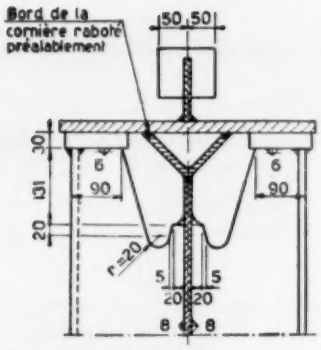


Fig. 8 Details of the upper flange showing grips for concrete floor on top of the flange. The edges of the angle are machined to fit the flange closely

square inch. The upper flange is 17.7 in. wide, 0.79 in. thick in the vicinity of the supports and 0.55 in. thick between the supports. The lower flange has the same width, but is 1.34 to 2.52 in. thick. At the junction of a thick- with a thin-flange plate, the thicker is tapered down on a slope of 1 in 20. The web is 104 in. high and $^1/_2$ or 1 in. thick (Figs. 5 and 6). Angles 4 x 8 x $^1/_2$ in. are used for all stiffeners (Fig. 7). Holes are drilled in the web at the start of the stiffener welds to decrease rigidity. The vertical stiffeners are welded to the web with continuous fillets. The start of each weld is elongated to reduce abruptness of section change. The lower end of each vertical stiffener rests against the tension flange. The upper end is blocked to the upper flange (Fig.8). Some of the welds were radiographed.

The Tannwald Bridge is the largest welded steel railway bridge in Switzerland.

New Life for Worn Surfaces

by D. E. Lytle

PUTTING a hard, long-wearing surface on a new or worn piece of equipment is an easy, profitable sideline for the welding shop. The job is done quickly, yet it will please the customer and will probably lead to more business.

Resurfacing a plowshare, pictured here is a common job of this type. A rugged surface that will outlast the original surface many times can be put on this plowshare with Haynes 92 hard-facing rod. This is the easiest hard-facing rod to apply, since it requires comparatively little heat, yet it is one of the hardest iron-base alloy rods and will give excellent service.

Preparation is the first step in hard facing. Dirt, grease, rust, scale and other foreign matter must be removed from the base metal before hard facing. If these impurities are not removed they will cause pinholes in the new surface. In the case of plowshares and similar equipment, there is another part of the preparation that is equally important. Unless it is new or unused, the plowshare must be sharpened before it is hard faced. Hard-facing material does not give shape to the part, so if the plowshare is dull before hard facing it will be dull afterward.

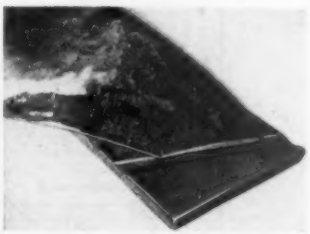


Fig. 1 The hard-facing job is under way here. Cleaning, sharpening and preheating a spot the size of a nickel have been completed

D. E. Lytle is connected with Linde Air Products Co., Pittsburgh, Pa.

Fig. 2 This plowshare is ready for hard facing. The nose has been slightly blunted and a bevel has been made on the landside edge

Fig. 3 More rod metal is being added to the molten puddle as the operator moves the puddle along the plowshare blade with the flame

After the plowshare has been sharpened grind the tip of the nose slightly flat so that the hard-facing material will have a supporting surface when it is applied to the tip. A bevel, about 1 to $^1/_2$ in. long, should also be made on the landside edge of the nose. This bevel is filled with hard-facing material, and it helps to maintain the side suction.

Preheating is the next step. The base metal is heated until it is a dark red, slightly below the sweating stage. Use an excess acetylene flame when preheating. Adjust the flame so that the acetylene feather is approximately one and a half times the length of the inner cone.

The preheating and the hard facing can be done by laying the share flat on a welding table, as the operator is doing in the photographs illustrating this article.

In preheating, the welding torch is held at a 30- to 60-deg angle to the base metal, with the inner cone about $^1/_8$ in. from the metal surface. Heat the plowshare until a $^1/_4$-in. area of the metal around the acetylene feather turns a dark red. Then dip the hard-facing rod in flux—Brazo flux is being used here—and draw back the flame slightly so that the end of the rod can be inserted between the flame and the metal.

If the melted rod foams or bubbles on the surface of the part being hard faced, the base metal has not been heated enough. Withdraw the rod and heat again for a few seconds. Then insert the rod again. The rod will melt and form a puddle when the base metal has been heated sufficiently.

Start hard facing at the nose of the plowshare. Guide the puddle, formed by the molten hard-facing material, with the flame. As the puddle is moved along the nose add to it at intervals by melting more rod, spreading the hard-facing alloy evenly with the flame. Do not spread it with the rod. Spreading or stirring the puddle with the rod prevents a smooth deposit.

As you move the puddle with the flame, the flame preheats the base metal in front of the puddle so that the hard-facing material spreads smoothly. The flame also keeps the edges of the puddle molten, and allows you to do the hard-facing job in one operation.

After the nose is completely hard faced, move the puddle along to the edge of the share. Care must be taken to avoid overheating the thin edge of the blade. Hold the blowpipe about $^3/_8$ in. in from the edge, and tilt it to a 45 deg angle to the edge. In this way the flame is spread out over the thin edge, and will not burn it. About a $^1/_4$-in. wide strip of hard-facing material approximately $^1/_{16}$ in. thick, should be deposited along this edge.

Pinholes and defects that appear should be removed before the molten puddle is moved more than an inch and a half or so away when possible. The flame should be moved quickly back to the defect, and the metal around it heated until it flows down into the defect and fills it completely. This will float off the impurity. If the defect is noticed

Fig. 4 The operator is taking care not to apply too much heat to the blade edge as the job nears completion

Fig. 5 This is an extra finishing job. The plowshare has been turned over and the underside of the nose is being hard faced before resharpening

on the finished job, then the area for several inches around the defect should be preheated to about 200°F. before the edges of the defect are melted and the defect filled in. If the surrounding area is not preheated cracks will appear.

Pinholes can also be caused by too slight an excess of acetylene, applying the hard-facing rod before the base metal is sufficiently heated, or removing the flame too suddenly from the molten hard-facing puddle. Occasionally cracks will also appear, but these are only surface cracks and are not in the base metal itself. These cracks and the pinholes not caused by impurities can be corrected by heating, or preheating and heating, and adding more hard-facing rod when necessary.

Finish the plowshare hard-facing job by sharpening with a medium soft emery wheel. Grind the landside first, then the underside of the nose. Then grind the top of the nose until you obtain a sharp edge.

That is how a plowshare is hard faced. Essentially the same method is used for hard facing any other piece of equipment. Any point of wear, or any surface subject to a considerable amount of sliding friction, can be protected by hard facing. It is simple to do, and it is sure to satisfy your customers.

Rail-Cutting Aids Road Improvement

OXY-ACETYLENE cutting equipment has been used to cut off raised sections of 10,037 ft of street-car track in Monaca, Pa., before the town's main street was repaved. Because of low curbs, the street elevation could not be increased by covering the tracks, and the cost of digging up the rails was prohibitive. By cutting off that part of the rail which protruded above the street surface and applying a thin layer of pavement, Monaca obtained a smooth surfaced road at minimum cost and with slight increase in elevation.

Cement filled the rail grooves from a previous attempt at street improvement. This was chipped out with pneumatic hammers. Then the cut was started with a hand-cutting blowpipe. An Oxweld CM-30 Cutting Machine continued the cut at an angle which would remove the largest section of rail. Cutting speeds of from 8 to 12 ipm were maintained, depending upon the condition of the rail groove.

A private cutting and welding shop contracted for the job and finished cutting and removing the track in 23 working days. Two cutting machines and one hand-cutting blowpipe were used. The operation consumed an average of 5000 cu ft of oxygen and 870 cu ft of acetylene per day for totals of 115,000 cu ft of oxygen and 20,010 cu ft of acetylene for the entire job.

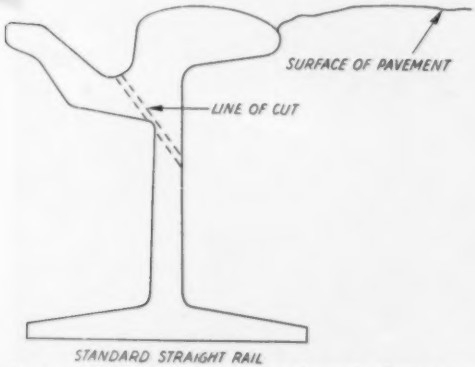


Fig. 1 Cross-section view showing the angle of cut to remove entire top of the rail

Fig. 2 Oxweld CM-30 Cutting Machine cutting street railway tracks. By using two sections of cutting machine track, cutting can be continuous

Back to Work Because of Braze Welding

by R. L. Fuller

SCRAP steel is badly needed by steel mills to make new steel. And an important amount of scrap comes from junked equipment such as machinery, automobiles and railroad equipment. This material should be moved to the scrap dealers to help ease the scrap shortage.

But, first, make sure it is scrap. Many shops have learned how to make good use of what others call "scrap." To them it is good, usable material.

Broken and obsolete machinery can be repaired or rebuilt by welding and cutting. These pictures show

R. L. Fuller is connected with Linde Air Products Co., New Orleans, La.

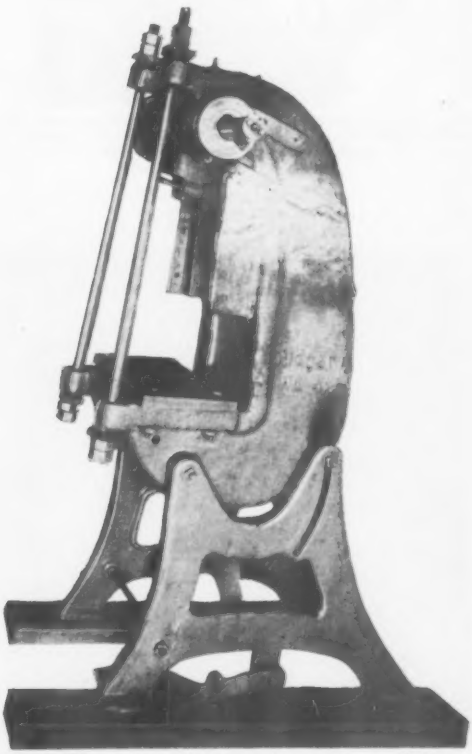


Fig. 1 Here is a cast-iron punch press frame that was repaired by braze welding. A missing piece was replaced with a steel insert and then welded to the frame

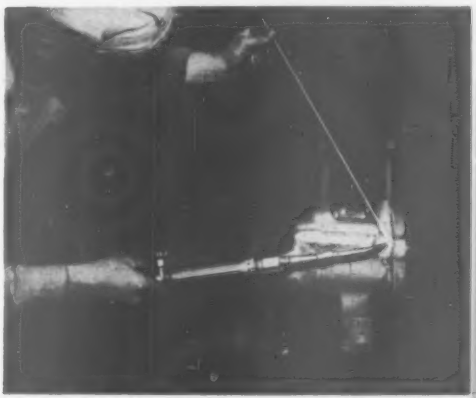


Fig. 2 When this water pump cracked in several places the cracks were veed out, then cleaned thoroughly with a wire brush. Here the braze-welded repair is being completed

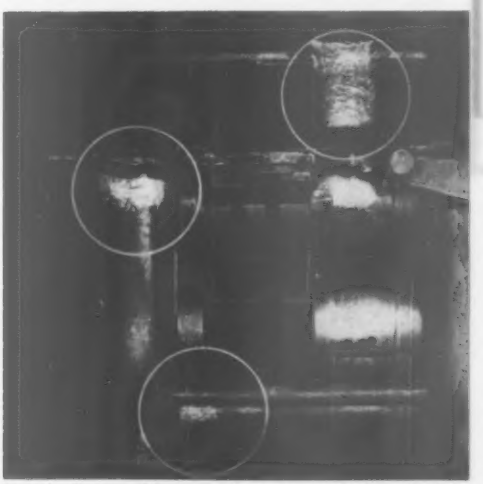


Fig. 3 Braze welding is a fast, economical method for repairing broken parts. Here are three of five welds that returned this rolling machine housing to service

some repair jobs that put a useful and important piece of equipment back to work. They were braze welded with an oxy-acetylene welding blowpipe and returned to service in a couple of hours.

Braze welding is fast because little or no preheat is needed and the repair can often be done without dismantling the machine. Braze welding, done under proper procedures, produces a repair that is as strong as the original part—and it saves valuable steel.

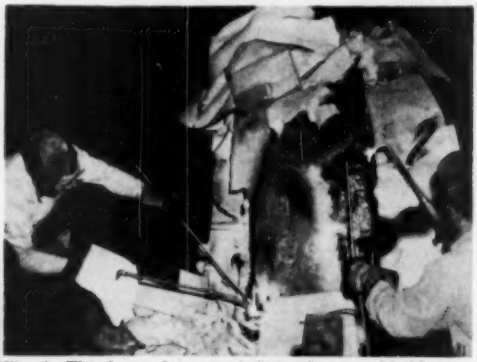


Fig. 4 This large shear cap is being braze welded by two operators. Broken pieces were chipped before welding began

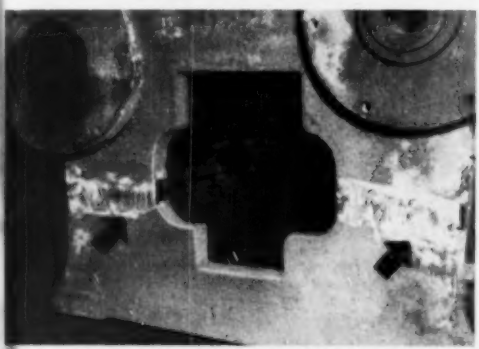


Fig. 5 Braze welding of this casting took only 12 hr. A new part could not be obtained for 9 weeks. Braze welding had it back on the job in less than a day

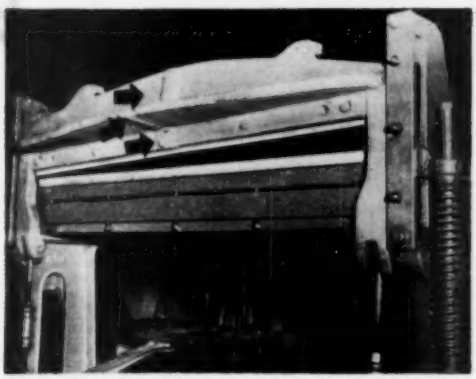


Fig. 6 This shows the rear section of a cast-iron shear press that was repaired by braze welding. The job was completed in 1½ hr.

Fig. 7 This fly wheel broke in three pieces. It was a simple matter to line up the parts, make tack welds, and then do a strong lasting braze-welded repair

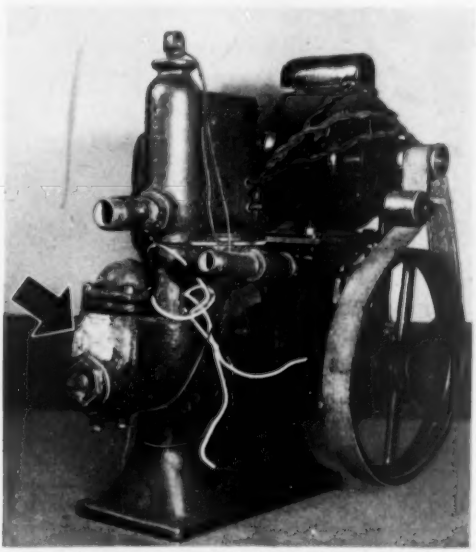


Fig. 8 The cast-iron part of the impeller of this electric pump cracked. The parts were veed and the weld was made with Oxweld No. 25M bronze rod

Tow Carts Fabricated by Inert-Gas Metal-Arc Welding

TEXTILE mill tow carts, used to transport nylon thread in the mill, are being fabricated by inert-gas metal-arc welding by one manufacturer. Both manual and automatic welding is used to make the carts. The Heliarc welds are smoother and require much less finishing than any other applicable welding process. The service in which these tow carts are used requires a completely smooth inside surface on the container.

Stainless steel is used for the cans since it will not rust—even if wet-dyed thread is transported. Any corrosion on the inside cart surfaces will damage the thread. The stainless steel cans, 22 gage by 40 by 42 by 50 in., are mounted on a galvanized steel base, Fig. 1. Twenty carts are made in one day. This includes some grinding on inside corners of 3-in. flared pieces welded to the top of the tank.

The first step in fabricating the carts is to cut out the parts that will make up the cans. Then the side seams are welded automatically, Fig. 2, and the top of the can is flared. Following this, the vertical corner seams are welded, also automatically.

Automatic welding is done on a jig fabricated by the manufacturer. It is a horn-type jig, with a grooved copper backing bar actuated by a pneumatic fire hose setup.

No welding rod is used to make the automatic butt seams. Speed of welding is 25 ipm, using a $^1/_{16}$-in. diam tungsten electrode at about 100 amp.

(Courtesy of Linde Air Products Co.)

Fig. 1 Tow carts fabricated by inert-gas metal-arc welding

(Courtesy of Linde Air Products Co.)

Fig. 2 Making of automatic butt welds in stainless steel cans

The can bottoms are welded manually. A lightweight Heliarc torch is used. Can bottoms are tacked in place first, then welded. Because of the high speed of welding, and the type of corner joint used, this work is done unjigged. Practically no distortion occurs.

After welding, sample cans are given ferrocyanide precipitation, and air pressure-soapsuds tests. All the samples taken thus far have passed satisfactorily. Out of a total of 16,000 ft of welding so far (7500 ft automatically) only one joint was spoiled, and this was due to welding without the argon shielding gas turned on.

the AMERICAN WELDING SOCIETY

activities • related events

Western Metal Congress

The Eighth Western Metal Congress and Exposition will be held in the Pan-Pacific Auditorium, Los Angeles, Calif., March 23rd through March 27th. The AMERICAN WELDING SOCIETY, through its West Coast Section, is contemplating interesting sessions, details of which will be announced as soon as possible.

Specifications for Copper and Copper-Alloy Welding Rods

The latest specifications for filler metal, issued jointly by the AMERICAN WELDING SOCIETY and American Society for Testing Materials (AWS Designation A5.7; ASTM Designation B259), covers copper and copper-alloy welding rods for use with oxy-acetylene, carbon-arc and inert-gas metal-arc (nonconsumable electrode) welding.

The twelve classifications established by these specifications include copper, silicon bronze, phosphor bronze, copper-nickel, naval brass, manganese bronze, low-fuming bronze, nickel bronze and aluminum bronze welding rods.

As with the other filler metal specifications for copper-alloy electrodes and for steel and aluminum filler metal, these new specifications specify the chemical composition, mechanical properties and usability characteristics of each classification, with corresponding tests for verifying these properties.

An appendix is included as an aid to users in selecting the best welding rod for their needs and using it in the most efficient way.

Copies of the Specifications for Copper and Copper-Alloy Welding Rods can be obtained at 40 cents each from either the AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y., or American Society for Testing Materials, 1916 Race St., Philadelphia 3, Pa.

O. B. J. Fraser Wins Miller Medal Award

The Awards Committee has awarded the Samuel Wylie Miller Memorial Medal for 1951 to O. B. J. Fraser whose studies of field applications and promotion of

welding research have contributed fruitfully to bridging the gap between welding art and welding science, and whose persuasive influence in professional circles has helped others to a better understanding of the science of welding for the practice of the art.

O. B. J. Fraser is Assistant Manager of the Development and Research Division of The International Nickel Co., Inc., New York. Born in Brookville, Ont., he received a B.Sc. degree in metallurgical engineering in 1916 from Queen's University, Kingston.

Mr. Fraser joined The International Nickel Co. in July 1917 as metallurgical engineer at the Bayonne, N. J., Works. In April 1918 he was transferred to the company's Port Colborne, Ont., Works and became, successively, superintendent of the Electrostatic Fume Precipitation Plant, night works superintendent and research engineer. During 1922, while still with the company, he became associated with the Mellon Institute in the study of corrosion of nickel and nickel alloys. Mr. Fraser was placed in charge of the Research Laboratories of INCO at Bayonne in 1924, remaining in that position until 1932. During the next two years he carried on field development work in the use of nickel alloys in the petroleum industry.

Mr. Fraser is actively identified as both officer and member of committees of a number of metallurgical and engineering organizations. He is a member of the AWS-ASTM Committee on Filler Metal.

Mr. Fraser joined the AMERICAN WELDING SOCIETY in 1935 and served as Treasurer of the SOCIETY (1941–1947) as well as Second and First Vice-President and President, respectively. Mr. Fraser is active in a number of other engineering societies.

RWMA Prize Winners

There is given below a brief biography of some of the authors of prize-winning papers. The biography of other prize winners will be published in later issues of the JOURNAL.

Paul Duker

Paul A. Duker was one of the joint authors of the paper that won first prize from an industrial group. He is a graduate from the University of Detroit with a degree of B.E.E. in 1936. He has been

employed by the Detroit Edison Co. in various capacities and since 1940 has served as Sales Engineer. Mr. Duker served as a Lieutenant in the U. S. Navy assigned to the Bureau of Ships. He is a member of Tau Beta Pi.

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Moving larger stones	IMPACT & abrasion	G-E W-94 & G-E W-96
Rock crushers, dipper teeth, tractor grousers	IMPACT & abrasion	G-E W-94 & G-E W-96
Railroad frogs & switches	IMPACT & abrasion	G-E W-97
Food, chemical handling equip.	CORROSION & abrasion	G-E W-97

NOTE: G.E. also manufactures lightly-coated hardfacing electrodes, and electrodes that give a flame-hardenable deposit.

For complete information on how you can increase the life of heavy equipment, see your nearest authorized General Electric Welding Distributor. He also carries a complete line of other G-E electrodes, accessories, and welding equipment.

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Myron Zucker

Myron Zucker, one of the three joint authors of the paper on "A Case of Power" that won first prize, is representative for Precision Welder and Flexopress Corp. in Detroit area, and consultant on resistance welding problems—especially power supply.

This is an outgrowth of years with Mackworth G. Rees Co. and Uniflex Cable Co., in both of which he was Vice-President. Previously 13 years were spent in the Engineering Division of The Detroit Edison Co. after four years on test and electronic-power application at General Electric Co.

Mr. Zucker is a graduate of Cornell University with a degree of EE and Union College with M.S. in E.E. He is a member of the AWS; Educational Committee, AWS Michigan Section; Member AIEE; Past-Chairman, AIEE Welding Committee; Chairman, Power Supply subcommittee; Governor, Electrical Assn. of Detroit; charter member, Engineering Society of Detroit; and Registered Professional Engineer in Michigan.

Mr. Zucker is active in civic affairs in his home city.

Peter G. Poetto

Mr. Poetto is author of the paper on "Quality Control for Spot Welding" which won second prize. He is Welding Engineer for the CBC Welding Corp. of New York City. Mr. Poetto is 44 years old, was born in Turin, Italy, and came to the United States in 1914. He is a graduate of St. John's University in Brooklyn with a degree of B.S. majoring in chemistry. He also took courses in Manhattan College, at Columbia University, New York University and Pratt Institute.

Mr. Poetto started as a welder in

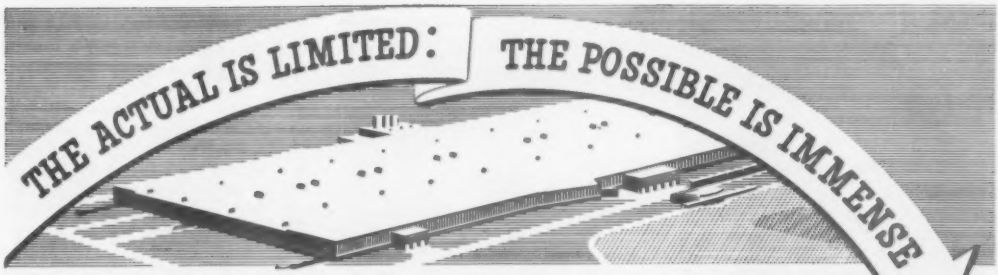
1930 and later became foreman of a welding shop. He was employed with the Edwards Metal Products Corp. of Long Island City, New York, in charge of welding steel assemblies. From 1941 to 1943 he was Welding Engineer at General Aircraft Corp., Astoria, Long Island, in connection with the welding of transport gliders. From 1943 to 1945 he served with the Air Force Materiel Command as representative of materials and processes. In 1945 he was Metallurgist in charge of materials and processes at Warren McArthur Corp., Bantam, Conn. From 1947 to 1949 Mr. Poetto was Welding Engineer of Pratt Whitney Aircraft, East Hartford, Conn., and from 1949 to 1952 he rejoined the Air Force Materiel Command as materials and process engineer. He is currently welding engineer in charge of quality control program for the Resistance Welding Department of the CBS Welding Corp., New York City. Mr. Poetto is married and has four children.

Prize Winners

Third prize in the 1952 RWMA Award Contest was won by a group of Pullman-Standard Car Manufacturing Co. engineers for their paper on "Corrosion of Structural Spot Welds." There follows a brief biography of the three authors.

B. Karnisky

Ben Karnisky, co-author of the paper winning third prize from an industrial source, graduated from Bicknell High School, Bicknell, Ind. He received his Bachelor's degree in 1938 and attended one year of postgraduate work at Purdue University, majoring in metallurgy. In addition, while employed by the Army Ordnance, he completed a special course in welding at Watertown Arsenal and M.I.T. His experience has been princi-



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By **B. L. Kapp**, Plant Manager & Chief Engineer
Corson Brothers, Inc.
Indianapolis, Indiana

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In addition to components shown in Figures 1 and 2, the main frame was also converted to welded steel. In place of a solid cast rectangular member, the main frame is now formed from 12 gauge steel with rolled edges for rigidity. The steel design is easier to assemble and has greater sales appeal.

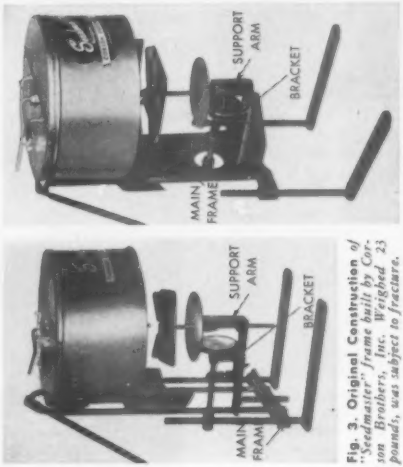

Fig. 3. Original Construction *of "Seedmaster" frame built by Corson Brothers, Inc. Weighed 23 pounds, was subject to fracture.*

Fig. 4. Present Welded Steel Design. *Steel members replace cast components, prove to be lighter, but stronger in construction yet are formed from 12 gauge sheet.*

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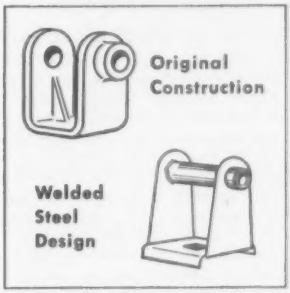

Fig. 1. Former Design *of bracket was gray iron . . . is now fabricated from 12 gauge steel. Steel design is rugged, durable, will not fracture.*

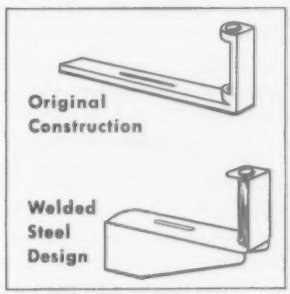

Fig. 2. Support Arm *originally a casting, required costly milling and drilling. Wall section was ½". Is now formed from 12 gauge material, has greater strength, increased rigidity, less weight.*

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pally in the welding and metallurgical engineering fields at Inland Steel, Sciaky Brothers (resistance welder manufacturers), Armour Research Foundation and various plants of Pullman-Standard Car Manufacturing Co. During the war he was Assistant Chief Inspector at the Pullman-Standard Plant in Hammond, Ind., supervising X-ray, magnaflux, layout, machining and welding inspection of M-4 tanks, 155-mm guns and other war materiel. His later experience with Pullman-Standard was in the manufacture of railroad passenger car and freight car equipment. Currently he is employed as Welding Research Engineer in charge of welding research and development for Pullman-Standard. Mr. Karnisky is a member of the American Society for Metals and the AMERICAN WELDING SOCIETY. Recently he presented an article on the progress of welding which was published in Metal Progress.

E. Kinelski

Eugene H. Kinelski, co-author of the paper winning third prize from an industrial source, was born in East Chicago, Ind., Nov. 3, 1918. He received his Bachelor's degree in Metallurgical Engineering from Purdue University in 1942. Prior to graduation in 1942, he had two years experience in the metallurgical department of the Inland Steel Co. Shortly after joining the U. S. Navy in 1942, he was assigned to research and development on torpedoes. He also spent one year on a secret project which included a short tour of duty in the Pacific. He went from torpedoes in the Navy to ram jets in civilian life at the Applied Physics Laboratory of Johns Hopkins University. There he was responsible for development, fabrication, inspection and testing, high-temperature metallurgy, etc., of a ram jet, propulsion test vehicle. Mr. Kinelski joined Cornell Aeronautical Laboratory, Inc., in Buffalo, N. Y., in 1947 to work in research and development in metallurgy of aircraft materials. He was engaged in welding, soldering and brazing of aluminum and titanium; galvanic corrosion studies; aluminum casting alloys; etc. After three years, he joined the Research and Development Department of Pullman-Standard Car Manufacturing Co. where he has been engaged in corrosion studies, protective coatings and metallurgy. Also, he recently joined the staff of Purdue University Calumet Center in Hammond, Ind., as a part-time instructor in metallurgy. He is still active in the U. S. Naval Reserve being a member of a research unit, in addition to being a member of the American Society for Metals and the National Assn. of Corrosion Engineers.

E. P. Gruca

The third author, E. P. Gruca, Head, Materials and Welding Section, was born in Chicago, Ill., Oct. 4, 1919. He received his B.S. in Chemical Engineering from Armour Institute of Technology (now Illinois Institute of Technology) in 1940. Mr. Gruca was awarded a fellowship in the Sloan Fellowship Program for Executive Development and received a S.M. in Business and Engineering Administration after a year's study at Massachusetts Institute of Technology in 1951.

His industrial experience has been com-

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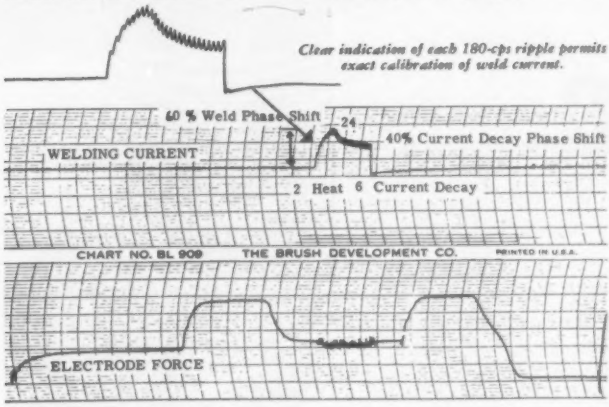
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pletely with one company, Pullman-Standard Car Manufacturing Co. in the Chicago area and in the field of welding research and welding engineering. Mr. Gruca's initial phase of industrial experience was spent as a metallurgist in spot welding research as related to railroad passenger car applications during which time he was a co-author on two RWMA prize papers. During the war, his responsibilities broadened to take in welder training and the development of automatic welding procedures in shipbuilding construction. In the postwar period, through 1949, he was engaged in setting up and controlling all phases of welding in railroad car construction, heading the Pullman Car Works Welding Engineering Department during 1948 and 1949. He was transferred to the newly formed Pullman-Standard Research and Development Department in Hammond, Ind., to head the Welding Research Section in 1950 and shortly thereafter, spent a year at MIT attending the Sloan Program. Mr. Gruca returned to Pullman-Standard's Research and Development Department where he has since headed the New Products Appraisal Section, and more recently, the Materials and Welding Section. He is a member of Alpha Chi Sigma, Phi Lambda Upsilon (Honorary chemical), AMERICAN WELDING SOCIETY and American Society for Metals.

Lincoln Gold Medal

The 1952 Lincoln Gold Medal was awarded to Howard S. Avery for his paper on "Hard Facing for Impact." There follows a brief biography on Mr. Avery.

Howard S. Avery was born in Canon City, Colo., in 1906. Moving to Virginia at the age of eleven, he went through

high school in Roanoke and college at Virginia Polytechnic Institute, from which he received a B.S. degree in 1927 and an Engineer of Mines degree in 1928, with honors.

Following several years of mining engineering and geology in Mexico, steel mill experience, and teaching, he joined the newly organized Metallurgical Department of the American Brake Shoe Co. in 1934. Since then he has been identified with this company's research in various aspects of industrial wear.

He married Louise Steele of Roanoke, Va., in 1931. They are raising a family of four children. Nonprofessional activities include supervising a junior rifle club, training Boy Scout leaders, and operating amateur radio station W2HBH.

Heat- and wear-resistant alloys have engaged a large share of his attention. His efforts to define the engineering properties of the most important industrial cast heat-resistant alloys are reflected in a series of technical publications. For years he has been a member of the Alloy Casting Institute Technical Research Committee, which guides cooperative research of the alloy casting industry, and from 1947 to 1950 was Chairman.

Professional society affiliations include the AMERICAN WELDING SOCIETY, The American Society for Metals, The Metal Science Club, The American Institute of

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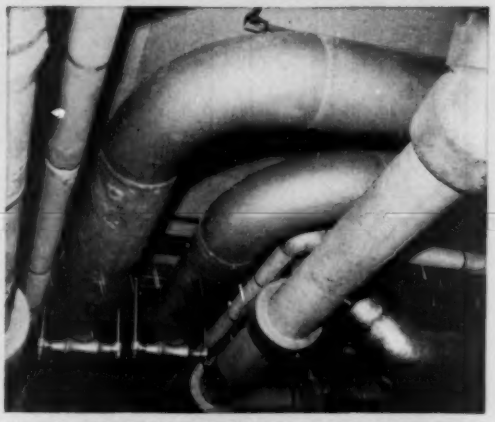

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Mining and Metallurgical Engineers, and the British Iron and Steel Institute. He has served on various specification, program, handbook and educational committees; and is on the Welding Research Council High Alloy Committee. He contributed to the 1950 WELDING HANDBOOK, the *1949 ASM Metals Handbook*, and is now chairman of the AWS-ASTM Filler Metal Subcommittee on Hard Facing Alloys.

Abrasion testing and the relative performance of the many alloys used to resist wear, from very tough austenitic manganese steel to the very hard martensitic irons, have been fields of continual study. This has naturally involved their use as welded overlays, which has received increasing attention because of his faith in the potential value of the hard-facing technique to industry. As research metallurgist for the American Brake Shoe Co., he is now actively planning and supervising research in this area. Significant results are being reported to the profession as a series of technical papers whose objective is to place hard facing on a sound engineering basis. Two of these received the Lincoln Gold Metal Awards for 1950 and 1952.

RWMA University Prize Winners

There follows a brief biography of some of the authors of prize winning papers in the RWMA Contest from a University source.

John J. McCarthy

Mr. McCarthy was one of the joint authors of the paper winning first prize which was entitled "Temperature Distribution During the Flash Welding of Steel."

John J. McCarthy was born in Troy, N. Y., on June 24, 1923. He received his high school education at La Salle Institute. In September 1941 he entered Rensselaer Polytechnic Institute to pursue studies toward a Bachelor of Metallurgical Engineering degree.

Mr. McCarthy enlisted in the Army of the United States in October 1942 and served in the Ordnance Department later being transferred to the Corps of Engineers. He saw service in the European Theater as well as the Pacific Theater of Operations. He was honorably discharged with the rank of Staff Sergeant after three and one-half years of service. Returning to Rensselaer he received his Bachelor's degree in January 1948. He then became employed as an Instructor in Metallurgical Engineering while continuing his education. Mr. McCarthy received the Degree of Master of Metallurgical Engineering in January 1951 and is at present working toward his Doctorate in the same field. His society membership includes Sigma Xi, Phi Lambda Upsilon and the American Society for Metals.

Dr. Ernest F. Nippes

Dr. Ernest F. Nippes, co-author of the papers winning first and second prizes from a university source, was born in New York City, Feb. 1, 1918. He received his Bachelor's degree in 1938, his Master's degree in 1940 and his Ph.D. degree in 1942—all from Rensselaer Polytechnic Institute. He stayed with that Institution and served successfully as Instructor of Metallurgical Engineering, Assistant Professor and is at present Associate Professor of Metallurgical Engineering and also Supervisor of Welding Research. He has served as Consultant with some 15 concerns, being at present with the Adirondack Foundries & Steel, Inc., Watervliet, N. Y., and The Oak Ridge National Laboratory, Oak Ridge, Tenn. His Society membership includes Sigma

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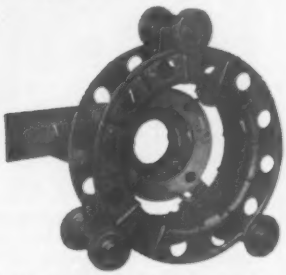

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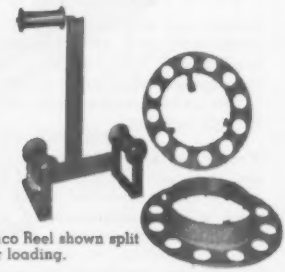

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Xi, Tau Beta Pi, Phi Lambda Upsilon, American Society for Metals, AMERICAN WELDING SOCIETY and American Institute of Mining & Metallurgical Engineers. He has published more than twenty papers in the field of physical metallurgy and particularly in electric arc and resistance welding.

John M. Gerken

John M. Gerken, joint author of the second prize-winning paper "Projection Welding of Steel in Heavy Gages and in Dissimiliar Thickness," was born in Jersey City, N. J., Sept. 3, 1920. He worked at the International Nickel Co., Research Laboratory in Bayonne, N. J., as a Junior Metallurgist from 1939 to 1942 and during the same time attended Newark College of Engineering, Evening Division. Mr. Gerken served three years with the army, in the Antiaircraft Artillery and later in the Signal Corps. He continued his education at RPI and received his Bachelor of Metallurgical Engineering degree in January 1949. Mr. Gerken remained at Rensselaer as a research engineer working on welding problems. He received his Master of Metallurgical Engineering degree in January 1951. He is a member of the AMERICAN WELDING SOCIETY, American Society for Metals, Tau Beta Pi and Sigma Xi.

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AWS provides every one in welding a common ground where he can meet people of his kind, learn more and improve himself faster than through any other method, including book learning. If each member brings a friend to an AWS meeting, one who is interested in welding, twice as many people would gain the knowledge brought out in the meeting. AWS Builds Men of Welding *if only we will let it.*

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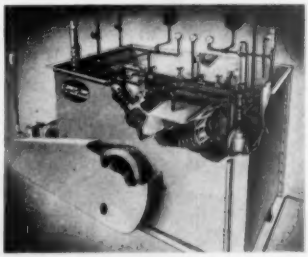

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abstracts of CURRENT WELDING PATENTS

2,608,138—Weld Bead Trimmer—Graydon B. Tener, Edgeworth, Pa., and Comer D. Hazen, Wheeling, W. Va., assignors to United Engineering & Foundry Co., Pittsburgh, Pa., a corporation of Pennsylvania.

This patented apparatus is for removing a weld bead from the seam in continuous welded tubing. The apparatus has a driven annular cutting means through which the tubing is passed for removal of weld bead metal, and driving means are provided to rotate such cutting means. The angle of contact between the cutting means and the tubing can be varied as desired.

2,608,446—Flux Feed Mechanism for Cutting Torches—John B. La Pota, Chicago, Ill., assignor to National Cylinder Gas Co., Chicago, Ill., a corporation of Delaware.

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In this apparatus, a cutting torch is provided and a closed flux powder tank is present. An ejector is connected by conduit means to the tank to receive flux powder therefrom by gravity flow and to deliver the powder upwardly into the ejector. Gas conduit means connect to the ejector to supply carrier gas for the powder. Control valve means deliver gas to the ejector at a predetermined pressure and deliver gas to the tank at a lower pressure, while gas pressure release means prevent the gas pressure within the tank from increasing substantially when powder is not being withdrawn therefrom.

2,608,887—Means for Cold Pressure Welding—Anthony Bagnold Sowter, Wembley, England, assignor to The General Electric Co., Ltd., London, England.

Sowter's pressure welding apparatus is for lap joining a first member to an angular flange of a second member. A first pressure welding tool means has a recess conforming to the bent formed by the flange resting upon the bottom of the recess. The cooperating welding tool is present for pressure welding the first member to the flange. The recess restricts and diverts metal flow around the bent and prevents outward splaying thereof during welding.

2,609,471—Welding Rod Holder—Harold B. Bergeson, Wilmington, Ill.

This patent is on a special welding electrode holder wherein a substantially tubular-shaped shield member is provided and has an opening extending through the side wall of a closed end portion thereof. Special means clamp an electrode in operative position in the opening with the electrode disposed in electrically insulated relation to the shield.

2,610,278—Pneumatic Welding Gun and Associated System—Harold J. Graham, Highland Park, Mich., assignor to Graham Mfg. Corporation, Detroit, Mich., a corporation of Michigan.

Graham's apparatus related to the percussion welding of a stud to a workpiece and a pneumatically operated welding gun is provided and has a piston reciprocatingly disposed therein. An electric power source is connected between the gun and workpiece and sequence controlling means move the piston until the stud contacts the workpiece to maintain a welding circuit supply energy to and maintaining said stud in contact with the workpiece until the welding is complete. Pneumatic means return the piston to its starting position upon completion of the weld.

2,610,326—Welder's Glove—Irene E. Sabin, Youngstown, Ohio, assignor to Sabin Co. Gloves, Youngstown, Ohio, a corporation of Ohio.

This welder's glove has a special back guard section provided thereon.

2,610,600—Portable Saw Welding Clamp—Earl F. Spencer, Vancouver, British Columbia, Canada.

This patent is on a special portable clamp for use in securing portions of a saw blade in desired relationship so that they can be secured together by welding action.

2,610,933—Brazing Flux—Louis E. Stark, Grand Island, N. Y., assignor, by mesne assignments, to Union Carbide and Carbon Corp., a corporation of New York.

The welding and brazing flux disclosed in this patent is substantially free of alkali metal compounds and contains 1 to 20% of zinc borate. The remainder of the flux comprises boric acid and incidental impurities.

NEWS OF THE INDUSTRY

1953 IAA Convention

The IAA 1953 Convention will be held in Atlanta, Ga. Headquarters will be the Biltmore Hotel. The dates are April 13th to 15th.

ASTM Executive Staff

The American Society for Testing Materials, through its President, Harold L. Maxwell (E. I. du Pont de Nemours and Co., Inc.) has announced the election of Robert J. Painter as Executive Secretary of the Society and of Raymond E. Hess as Associate Executive Secretary and Editor-in-Chief, both promotions effective September 16th.

This action was taken by the Board of Directors following a report of a special committee which had been appointed to recommend a successor to the late C. L. Warwick, long-time Executive Secretary of the Society, who died suddenly April 23rd.

Both men have been members of the ASTM for many years. Mr. Hess has been Assistant Executive Secretary and Editor, and Mr. Painter most recently has been Treasurer and Assistant Secretary.

The New Executive Secretary received his degree of Civil Engineer from Rensselaer Polytechnic Institute in 1928. Following a period on the RPI Faculty in the C. E. Department, he was in the Special Engineer's Office, Bethlehem Steel Co., and joined the ASTM Staff in 1931. Mr. Painter was born in upstate New York and attended the public schools in Endicott.

Mr. Hess, a native Philadelphian, is a graduate of the University of Pennsylvania in Civil Engineering. Following a short period with Day and Zimmermann, Inc., he became a member of the ASTM Staff in 1920.

20th National Power Show Covers Applications in Every Industry

Every going industry is included in the scope of the 20th National Exposition of Power and Mechanical Engineering, which will be held in Grand Central Palace, New York, December 1 to 6, under the auspices of the American Society of Mechanical Engineers. The event coincides with the ASME annual meeting, and many of the exhibits at the show will exemplify subjects under discussion by the engineers.

The exposition covers all branches of the field of power from the conversion of energy into fluid forms, such as heat, light, flowing water and steam, as well as hot, cold and compressed air, to its application in moving mechanisms. It includes the conversion processes themselves such as the combustion of fuels, generation of electricity and the operation of heat exchangers, pumps and blowers; distribution in transmission lines and pipe lines, and it includes all the many applications within the industrial plant, through wires, piping, shafting and belts, whether or not it generates its own power or buys it from a public utility.

Attendance is limited to persons having a professional, scientific or personal interest in the exhibits. Visitors are admitted by invitation and registration, but without registration fee, while the general public is excluded.

American Society for Metals Gives Three $2000 Teaching Awards in Engineering

As a direct aid in the teaching of more qualified engineers needed by industry, government and private research, the American Society for Metals is this year

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- Eliminates need for a barrier layer of nickel weld metal between steel and Monel overlays.
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Although not recommended specifically for that purpose, *"140" Monel* electrodes may be used to weld solid Monel in place of the standard *"130" Monel* electrode.

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Current requirements: **D.C., reversed polarity**

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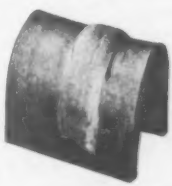
5/32" dia. — 110 - 150 amps.
3/16" dia. — 140 - 190 amps.

Properties of welds:

Joint	Tensile P. S. I.	% Elong.
Monel to mild steel	70,100	45
Nickel to mild steel	64,600	49
Monel to Type 316 stainless	87,800	44
Monel to Type 347 stainless	81,100	48



Guided bend test specimen of welded overlay made with new "140" Monel electrode directly on 3" by 6" by 3/8" steel plate. Note the excellent ductility and adherence of the deposit.

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awarding three cash prizes of $2000 each for the three best jobs of teaching turned in during 1951–1952 in the field of Metallurgical Engineering.

W. H. Eisenman, National Secretary of ASM announces the three following winners have been selected by the awards committee to receive the awards this fall: Arthur A. Burr, Assoc. Professor, Department of Metallurgy, Rensselaer Poly-Technic Institute, Troy, N. Y.; Joseph W. Spretnak, Assoc. Professor, Department of Metallurgical Engineering, Ohio State University, Columbus, Ohio; and Robert D. Stout, Professor, Department of Metallurgy, Lehigh University, Bethlehem, Pa. Dr. R. D. Stout is an active member of the AMERICAN WELDING SOCIETY and Chairman of the University Research Committee of the Welding Research Council.

Established by ASM to stimulate greater interest in the best possible teaching performance and to increase the enrollment of students in this branch of engineering, the awards go to teachers who, upon documented records, have made vital contributions to their profession through outstanding teaching performances.

National High School Awards for Farm Arc Welding

The First Award of $600 in the second annual competition of the $7000 Farm Welding Award Program for High Schools, sponsored by the Lincoln Arc Welding Foundation of Cleveland, Ohio, went to a California high school boy, Marvin F. Ormonde of San Luis Obispo. The award was made for his description of a livestock rack for a truck and a bulldozer frame and scraper blade, both of which were welded as a school and home project. First Award last year also went to a California boy, Stan Reifel of Ontario.

The awards and honors distributed by the Foundation in this national competition went to 105 boys in 23 different states. Also honored were the high schools and farm mechanics teachers of the first ten award winners in honor of whom their teachers and schools were given certificates and cash awards for the improvement of shop courses. Awards were made to the boys for the best description of farm equipment and tools which they either made or could make at school or at home as a project using arc welding. Papers were submitted by boys from 39 different states.

In honor of the First Award to Marvin Ormonde, his school, the Arroyo Grande Union High School, also received $600 and his instructor, Herbert Brownlee, was presented a certificate.

The Second Award of $400 went to Donald Moore of Copeland, Kan., who described a home-made modern feeding

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system for handling ensilage to the large herd of cattle on his father's farm. The Copeland Rural High School and his teacher, Will M. Seacat, were honored with an equal cash award and certificate.

Third Awards of $200 each were given Billy Ray James of Clay Center, Kan., and Martin Gundrum of Theresa, Wis. They described, respectively, an all-purpose cattle squeeze and a self-propelled hay and corn chopper. Their schools, Clay County Community High School and Mayville High School, were equally awarded. Ray Marrison, teacher at Clay Center, had five successful students in the Program. Instructor at Mayville High School is Andrew Magyar.

Dr. E. E. Dreese, Chairman of the Board of Trustees of the Foundation, states the purpose of this series of award programs, sponsored annually by the Foundation, as "encouraging farm boys to study the use of arc welding in farm operation and maintenance."

In a recent survey of 20 agricultural schools it was the consensus of the deans and professors of agricultural engineering that it was possible for a farmer to save up to $500 a year by doing his own repairs and even up to $2000 a year by saving crops in emergencies; he could reduce the labor time required in farming by 25 or 50% through making labor-saving equipment; and that one out of every four farms in the country could profitably use welding equipment.

The Foundation is announcing a new, similar award program for the current school year, closing June 15, 1953. It is open to all high-school students who live on farms or ranches. The Rules and Conditions booklet, illustrated with pictures of winning projects, is available from the Lincoln Foundation, Cleveland 17, Ohio.

Two Princeton University Men Top List of Award Winners

Sidney Shore, Assistant Professor, and E. W. Rothfuss, Jr., Graduate Student, both of the Civil Engineering Department of Princeton University, shared a First Prize award of $500 under Category "A," "Welding Engineering and Theory," of the $2000 International Prize Competition for papers on "Technological and Research Aspects, Advances and Advantages of the Use of Lower Melting (Lower Than Parent) Filler Metals in the Non-Fusion Welding Process," conducted by Eutectic Welding Alloys Corp., Flushing, N. Y.

Winner of the Second Prize, of $300, under this category was H. Edward Flanders of the University of Utah.

Third Prize of $200 under the "Welding Engineering and Theory" classification was shared by Paul Broadstone and Fred Salmons of the R. G. LeTourneau Corp. of Longview, Tex.

Category "B" of Eutectic's $2000 International Prize Competition covered papers on practical welding applications and the First Prize in this category resulted in a three-way tie with $100 awards being given by the company to F. DiFrancesco, welder for an ice cream machine fabricator of Bronx, N. Y., Everett Ayers, Safety Motor Transit Corp. of Roanoke Va., and Fred W. Hellings, of Henry Disston & Sons, Inc., Of Philadelphia, Pa.

Air Reduction Sales Co. Names New Managers

The Air Reduction Sales Co. recently announced the appointment of A. S. Blodget, Jr., as manager of their Pittsburgh district. E. S. Twining, Jr., formerly assistant sales manager of the company's Philadelphia district, succeeds Mr. Blodget as manager of the Boston district. The changes were made following the death of S. D. Edsall, former manager of the Pittsburgh district, according to H. F. Henriques, vice-president. Both are AWS members.

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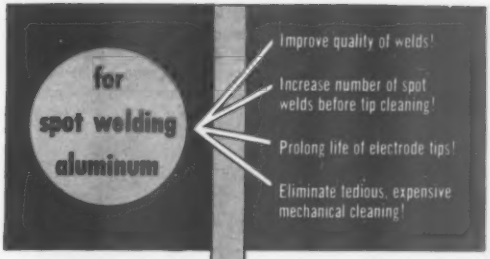
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J. H. Williams & Co. 1084

NEW LITERATURE

Bulletin on Hardenable Stainless Tubing Steels

Two economical stainless tubing steels having the attributes of resistance to corrosion and oxidation, and also the ability to be hardened by heat treatment to develop good mechanical properties, are discussed in a new technical data card issued by the Tubular Products Division of The Babcock & Wilcox Co. Known as Technical Data Card 143, the bulletin discusses B&W Croloy 12 (12% chromium steel—AISI Type 410) and B&W Croloy 12-2 (12% chromium, 2% nickel steel—AISI Type 414). Included are condensed technical data on corrosion resistance, mechanical properties, forging, machining, welding, heat treatment and physical properties.

Copies of Bulletin TDC 143 are available, free, on request to the company's offices at Beaver Falls.

Stainless Welding Chart

Armco Steel Corp. offers another handy wall chart through its distributors in principal cities.

This new two-color chart covers welding and soldering operations on stainless steel. It pictures and describes such practices as jigging to weld, oxy-acetylene welding, metal arc welding, inert-gas-shielded arc welding, spot welding and a sequence of photos on correct soldering practice. Included also is a welding reference table.

The chart can be obtained free from the nearest Armco Stainless Steel distributor, or by writing to the company in Middletown, Ohio.

Consulting Engineering Firm Brochure

To explain the variety of technical services it offers to industry, Sam Tour & Co., Inc., an organization of consulting and development engineers, has prepared a brochure entitled "All Under One Roof."

This two-color booklet describes the activities of the company's engineers, metallurgists and chemists in dealing with technical and industrial problems. These activities include research projects on new materials; development of products, processes and production methods; technical consultation; legal testimony based on investigations; and nondestructive testing both in the laboratory and in the field. Also described are the complete laboratory facilities for the comprehensive testing and analysis of all types of industrial materials.

Copies of "All Under One Roof" may be obtained by writing Sam Tour & Co., Inc., 44 Trinity Place, New York 6, N. Y.

Fundamentals of Physical Metallurgy

By Ralph Hultgren

This book consists of 395 pages copiously illustrated. Among several things it explains the phase reactions predicted from phase diagrams; accounts for the resulting microstructures and indicates the effects of microstructures on mechanical properties of alloys. Other special features are:

1. The exceptionally quantitative treatment makes possible an effective approach to a number of hard-to-present topics.

2. Four entire chapters treat laboratory procedures needed for the first-year course.

3. Many valuable topics include: The modern theory of martensite formation, dislocation theory, the future of titanium, laws of metallic crystal chemistry, deformation and recrystallization.

The price of this book is $9.35 per copy. It is published by Prentice Hall, Inc., 70 Fifth Ave., New York 11, N. Y.

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175	325	650	1200	1750
188	338	700	1250	1800
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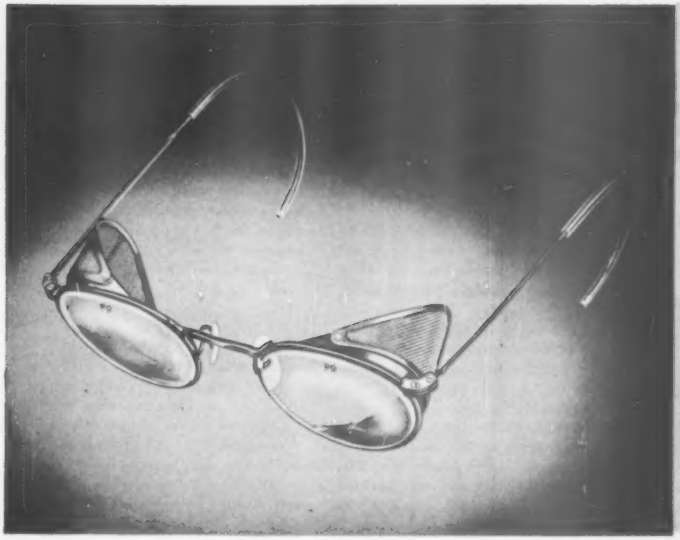

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The new 220 Series Goggles are designed to give the utmost in eye protection. Comfort is assured during continued wear by proper balance combined with lightness of weight. This results in worker approval and a higher factor of safety. Varied combinations make these goggles adaptable to many industrial operations requiring positive eye protection.

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New Bulletin on Croloy 18–8 Ti

Engineers associated with the design, fabrication and application of equipment using stabilized stainless steel tubing, will be interested in a new data card issued by the Tubular Products Division of The Babcock & Wilcox Co. Known as *Bulletin TDC-144*, it discusses B&W Croloy 18–8 Ti (AISI Type 321). Included are condensed data on corrosion resistance, creep strength, oxidation resistance, mechanical properties, machinability, welding characteristics, heat treatment and physical properties. Although popular in its own right, this steel recently has become more widely used due to the fact that the use of the columbium stabilized alloy, 18–8 Cb (AISI Type 347) has been restricted.

Copies of *Bulletin TDC-144* are available, free, on request to the company's offices at Beaver Falls, Pa.

Hard-Facing Guidebook Revised

The rebuilding and hard facing of all types of heavy equipment used in earth-moving, mining, lumbering, cement and allied industries is today considered almost standard procedure. The various materials used and the methods of applying these alloys are of prime interest to maintenance men.

Some years ago Stoody Co., manufacturer of hard-facing alloys, Whittier, Calif., published a booklet called "Stoody Hard-Facing Guidebook" which has gone through a number of editions since its first publication and has received nation-wide distribution. This book has recently been completely revised and is now available to those interested in the subject matter covered. It is obtainable through Stoody dealers throughout the United States and Canada or on request to the company.

Tempilaq°

Tempil° Corp., 11 W. 25th St., New York 10, N. Y., are offering a supplementary sheet of instructions for using Tempilaq° as a working aid to the growing number of users. "Instructions for Using Tempilaq°" contains much valuable information on how this temperature-sensitive liquid can most effectively be employed to determine attained temperatures. A number of special techniques are discussed. Among them are: recommended procedure for the use of a "faint haze" on glass (radio power tubes) or other highly polished surfaces; method of assuring adherence of Tempilaq° to fast moving or rotating surfaces, or to surfaces subjected to high gas velocities; the effect of evaporation of Tempilaq° at temperatures considerably higher than its assigned rating; adapting the thickness of the Tempilaq° coating to special conditions; and the effect of reducing atmospheres on the accuracy of Tempilaq°.

Safety Catalog

A 144-page catalog, titled "Everything for Safety," is announced by The Boyer-Campbell Co., 6540 St. Antoine St., Detroit 2, Mich. It describes face shields, welding helmets, "Supersight," "Skin-Cote" liquids and creams for protection against skin infections—safety clothing, machine guards, etc., anything required for a comprehensive safety program—a request on your letterhead will bring it to you without charge and postpaid.

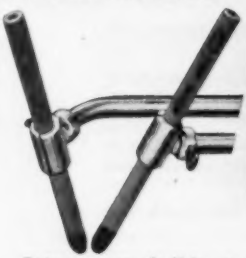
Buyers Guide

In this fast-moving era of metallurgical development, a buyers guide to alloys and fluxes for welding, brazing, soldering, tinning and cutting, that is older than the newest one, could put a manufacturer who has to buy such supplies behind the competitive 8-ball. Such is the spirit of the new *Buyers Guide* just being issued by All-State Welding Alloys Co., Inc., White Plains, N. Y.

A case in point is aluminum. Wide

BECKER

KEEN-ARC CARBONS



FOR CONSISTENTLY GOOD RESULTS

Designed to carry the high currents necessary for intense heat, BBB Keen-Arc Carbons produce a fine-grained weld of high tensile strength. They give a smooth, steady "flowing" flame which does not wander and which is concentrated at the desired focal point. Flame temperature is easily and accurately adjusted by merely changing the ampere input, and heavy copper coating permits gripping at extreme ends—eliminates frequent and periodic resetting.

A COMPLETE LINE OF CARBON WELDING SUPPLIES including carbon and graphite electrodes, carbon rods and plates, welding paste, etc. Write for catalog.

BECKER BROTHERS CARBON CO.

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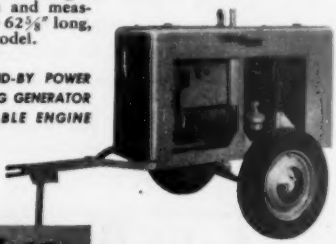
NEW ENGINE-DRIVEN D-C WELDER with A-C power take-off

The new Westinghouse Engine-Driven Welder was developed for construction and field-maintenance operations. It provides 200 amperes of d-c current for welding. It also provides 3,000-watt, 110-volt, a-c current for stand-by power. By simply plugging into convenient receptacles on the a-c power panel, the operator may obtain power for drills, grinders, pumps, lights and other electrical equipment.

The standard unit is skid mounted for truck transport or may be mounted on a high-speed, pneumatic-tired trailer. Ready for field service, the unit weighs only 1,150 pounds and measures 39½" high by 62⅜" long, in the stationary model.

★ CONVENIENT STAND-BY POWER
★ IMPROVED WELDING GENERATOR
★ EFFICIENT, RELIABLE ENGINE



J-21713

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FOR MORE INFORMATION
Write to the Westinghouse Electric Corporation, Welding Division, Buffalo, New York, or contact your nearest Westinghouse representative.

THE MOST PORTABLE GAS-DRIVEN D-C ARC WELDER EVER BUILT!

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Portable on the job... as well as to the job

Two men can lift it with ease

Fits in light truck or station wagon

On-the-job welding, quickly done

Ideal for 'round-the-plant maintenance

Rolls across railroad tracks—or rough terrain

Arc weld anywhere now —with ease, speed

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THIS is the only gas driven D-C welder that is light enough and compact enough to go *anywhere*.

One man can move it. Two men can lift it. It can be stowed with ease in a light truck, jeep or station wagon.

Not only can it be taken any place, but it will do a wide range of work and do it well. It can quickly pay for itself by cutting costs on hard-to-get-to welding jobs.

Special design and the extensive use of aluminum makes the Sureweld *light but strong*. Features such as the air-cooled engine, heavy-duty generator, dual-control, armature reactance and governed motor speed result in high performance and long dependable service.

It's Easy to Find Out for Yourself

You can get complete information about the Sureweld Welder from your nearest NCG district office or from the NCG authorized dealer in your city. Or use the coupon at the right to get descriptive literature and prices.

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substitution of aluminum for scarcer metals in many fields has so accelerated the technology of brazing, welding and soldering aluminum to aluminum and aluminum to other metals that in the All-State line of today there are two aluminum alloys for every one that was in the line a year ago.

Another case in point is the silver solders. The new *Buyers Guide* gives data on 12 All-State silver solders now. A year ago it was only six. Best buys can not be made without data on the full dosen.

There is *wanted* information in the *Buyers Guide* for all who have jointing, tinning or cutting to do which involves steel, aluminum and aluminum alloys, cast iron, copper and copper-bearing alloys, magnesium, nickel, stainless steel and zinc die cast.

Copies are available on request and without obligation.

New Bulletin on Lincoln School of Welding

A new bulletin has just been published by the Lincoln Electric Co., Cleveland 17, Ohio, descriptive of the Lincoln School of Welding, the various courses given and the School's facilities.

Art Madson, head of the Lincoln School of Welding, knows more welding operators than any other man in the world. Head of the School for 25 of its 35 years of continuous operation, Art is the unofficial head of an international brotherhood of welding operators who have graduated from the School. The 35,000 men and women who have gone through the School in Cleveland have come from 19 different countries, from as far away as India and South Africa. All have come under the friendly, patient supervision of this smiling Swede.

The School was started in 1917 when the United States Army purchased some welding equipment from Lincoln and specified in the purchase that Lincoln train soldiers to be welders. Since that time the school has been turning out professional welders for industry at a rate averaging better than 1000 a year. During World War II the School operated 24 hours a day with four separate classes being taught during each 24 hours.

The basic course at the Lincoln School consists of 4 weeks' training and practice with a new class starting every week. Lincoln conducts the School as a service to industry and only a nominal charge for materials is made. Advance courses in special welding techniques such as pipe welding and sheet metal welding are also available.

Butt Welders

A new booklet explaining how Solar butt-welding machines can cut costs and improve quality in fabricating smooth cylinders and in joining flat strips and sheets has been published by Solar Aircraft Co., 2200 Pacific Highway, San Diego 12, Calif.

Solar originally designed these welding machines for its own use in fabricating stainless and alloy steel parts for aircraft and engine components. After several metalworking concerns in the U. S. and in Europe saw the welds made on the machines and asked Solar to build similar units for them, Solar recently decided to offer two models of butt welders for sale to companies making precision sheet metal parts.

Solar butt welders provide highly precise and uniform welds of the inert arc type and are easily operated by semi-skilled workers. The specially designed work-holding clamps result in strong, ductile weldments, generally without the use of filler wire. Solar butt welders easily handle the usual variety of materials and thicknesses found in aircraft and industrial sheet metal shops.

Employment Service Bulletin

V-278. Engineer wanted for standards department. Knowledge of welding and/or metallurgy desirable, not essential. Location New York. Submit information on education, experience and salary desired for interview.

Sales Engineer, Arc-Welding Equipment

Sales engineer wanted to represent nationally known manufacturer of arc-welding accessories.

Applicant should have background and experience in metals and welding field. Liberal drawing account against commissions. Potential earnings $10,000 plus. Car needed but travel limited to metropolitan areas. Several select territories open.

Send recent photograph and complete résumé of education and experience. Personal interviews will be arranged locally for those who qualify.

Write Box V-279.

WELDING CONNECTORS

Saxe Welding Connection Units position and secure structural parts to be welded.

Clip K3A permits an adjustable connection.

These widely used units eliminate all hole punching, and, with welding, produce the most economical, safe, and quickly erected structural frame.

Write for 1951 edition, *Structural Welding Practice Manual*.

J. H. WILLIAMS & CO.
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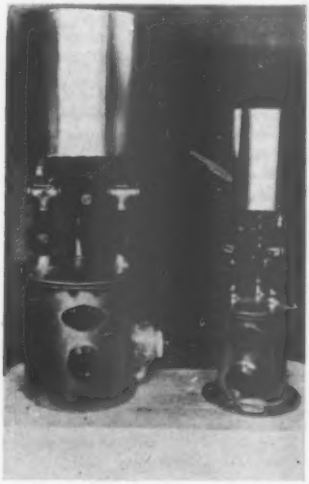
NEW PRODUCTS

New Gasfluxer Models

Two new model Gasfluxers are announced by the Gasflux Co., Mansfield, Ohio. One of these is the Model 68, having a much larger capacity than the most popular Model RE. As in the case of the Model RE, this unit eliminates the old methods of dipping brazing rod into flux or brushing flux on the work, and makes cleaning operations after brazing unnecessary. The Model 68 was designed primarily for use with city gas. It has $^1/_2$-in. orifices as compared to the $^1/_8$-in. orifices of the Model RE, and a 3-valve by-pass to regulate the amount of flux that is delivered to the flame. The reserve tank on this model is of 1-gal capacity.



Left, new Gasfluxer Model SFH. Right, Model 68 Gasfluxer

The other addition to the line is the new heavy-duty Model SFH Gasfluxer, which has capacity for fluxing 100 to 120 cu ft of fuel gas per hour and a 5-gal reserve tank. It is equipped with a 1000-watt heater to overcome the refrigerating action of the fuel gas bubbling through the liquid flux and to keep the flux at the proper operating temperature (100° F). Two thermostats, one on the bottom of the fluxer and one in the switch box, control the temperature.

Moly-Manganese Electrodes

Supplementing its widely accepted nickel manganese electrodes, Stoody Co. of Whittier, Calif., has announced the production of a moly-manganese material for manual welding and in coils for automatic electric application. The new electrodes contain 14–15% manganese, with deposits showing full Hadfield properties.

Physical properties of bare Stoody Moly-Manganese as deposited are as follows: Rockwell hardness 85–90 B, and as cold worked, 43–47 C; tensile strength, 100,000 psi; yield strength, 85,000 psi; elongation in 2 in., 14%; deposits nonmagnetic.

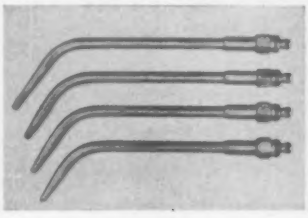
The moly-manganese electrode is designed primarily as a build-up material for manganese steel parts. Deposits of the bare rod possess maximum physical properties and have weldability closely approaching the conventional coated electrodes. Sizes are 14 in. length and $^5/_{32}$, $^3/_{16}$ and $^1/_4$ in. diameters. Prices of Stoody Moly-Manganese are slightly less than those of the nickel manganese.

Quick-Change Purox Welding Heads

Thirteen new welding heads that fit interchangeably on Purox W-202 and W-201 blowpipes have been announced by Linde Air Products Co., a Division of Union Carbide and Carbon Corp.

No wrench is needed to get a firm, gas-tight seal between the blowpipe handle and welding head. The new heads snap in or out of either blowpipe with one easy hand motion. Two synthetic rubber "O" rings *eliminate all metal-to-metal gas seals* between head and blowpipe, and make this quick-changing feature possible.

Practically any metal from 32 gage to more than one inch in thickness can be welded with the appropriate blowpipe by selecting the proper head. Head sizes are spaced so that welding ranges overlap and give complete coverage for work on any metal thickness with either a soft or hard flame. Each head is stamped with its size—a number from 2 to 100—that indicates the flow rate of each gas (oxygen and acetylene) in cubic feet per hour at recommended operating pressures.



Each head is made up of three components: mixer, mixer tube and pure copper tip. The mixer is of an improved disk-type design that resists flame pops and backfires, and eliminates burnouts. The mixer tube is extra short to provide additional flashback resistance by more efficient transfer of heat to the copper tip. All internal passages in the heat- and wear-resistant tips are swaged for better flame stability.

Machine-Cutting Speeds Shipyard Job

Machine cutting replaced hand cutting for one operation in a Cleveland shipyard and slashed the time needed to prepare steel castings for welding by almost 88%. Rough casting ends are now cropped and made ready for welding in 8 hours compared with the 66 hours required when hand cutting was used for the job.

This Oxweld CM-16 cutting machine, mounting a water-cooled oxy-acetylene blowpipe, is being used to crop the end of a large steel casting. The cutting machine runs on the twin track section at right to assure a straight cut

An Oxweld C-43 machine-cutting blowpipe mounted on a modified Oxweld CM-16 cutting machine does the actual cutting. Before this setup was installed, casting ends were cropped with a hand-operated oxy-acetylene blowpipe. This left cuts requiring considerable chipping and finish grinding before they were smooth enough to be welded. Hand cutting also caused problems of operator

Meet Government Specifications With
NEW SINGLE PHASE NON-SYNCHRONOUS
UP-DOWN SLOPE CONTROL
with
Robotron "CUSHIONED POWER" WELDING TIMERS

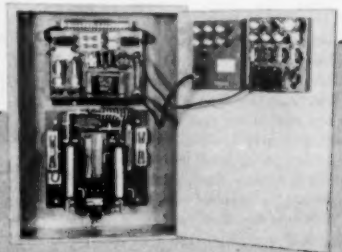
Now . . . with this new concept of Non-Synchronous SINGLE PHASE Control you can weld all types of Brass, Aluminum, Monel Metal, Stainless and Nickel Alloy Steels, Coated or Plated Metals or any combination of Dissimilar Metals and meet U. S. Government Specifications. Welding is accomplished with minimum surface indentation, and electrode pick-up, maximum ductility and constant uniformity. The Robotron Non-Synchronous SINGLE PHASE Control Unit has the simplicity of a single phase circuit instead of the present complicated and difficult-to-maintain multiple circuits.

HI-CONDUCTIVITY
LO-CONDUCTIVITY

Robotron's "Cushioned-Power" welding cycle begins with a PREHEAT, or up slope, stage that assures proper seating of electrodes, cutting down surface resistance and increasing WELD heat where it's needed. It also decreases surface indentation, spewing of metal and lengthens the life of the electrodes. The TEMPER stage, or down slope (Trailing Current), controls rate of heat flow into the surrounding metal after weld is over, definitely controlling brittleness and in case of Coated Metals, allows coating or plating to resurface the weld.

Non-Synchronous "UP-DOWN" Slope Welding Control Unit is the least expensive unit on the market today, and it still assures better welds under a greater variety of welding conditions.

And best of all . . . Robotron's well-known plug-in feature gives you quick, easy installation and maintenance, plus complete versatility and adaptability in operation.



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NEMA 3B WITH "UP-DOWN" SLOPE CONTROL —600 AMP. IGNITRON CONTACTOR AND SIZE "B" IGNITRON TUBES

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fatigue due to the time and effort needed to cut the heavy sections. Machine cutting does the job more rapidly, leaving smooth cuts which need little finish machining, and reducing operator fatigue and the delays which it causes.

Four of these castings form the 40-ton stern frame of a large Great Lakes ship. This frame provides rigid support for the propeller shaft, screw and the counterguide of the ship.

The frame cross section is triangularly shaped for streamlining. Cross-section thicknesses vary from 4 in. at the leading edge to 24 in. at the trailing edge. Cutting speeds through these cross sections vary from 1 to 15 ipm. Cutting oxygen pressure is carried at 45 psi.

New Equipment Replaces Galvanized Surface on Butt-Welded Parts

Due to the high cost of galvanizing after fabrication, it is desirable to fabricate parts, such as butt-welded tubing and containers, from galvanized steel.

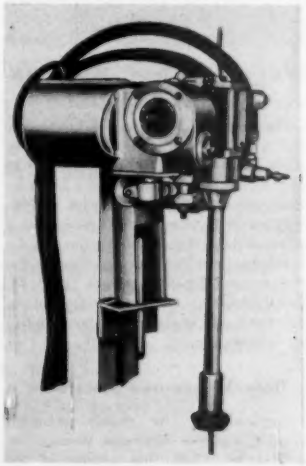
During the butt-weld operation the galvanized finish is burned off due to the high welding temperature.

The Metallizing Company of America, Chicago, Ill., after years of research, have developed a process for recoating the exterior burned area.

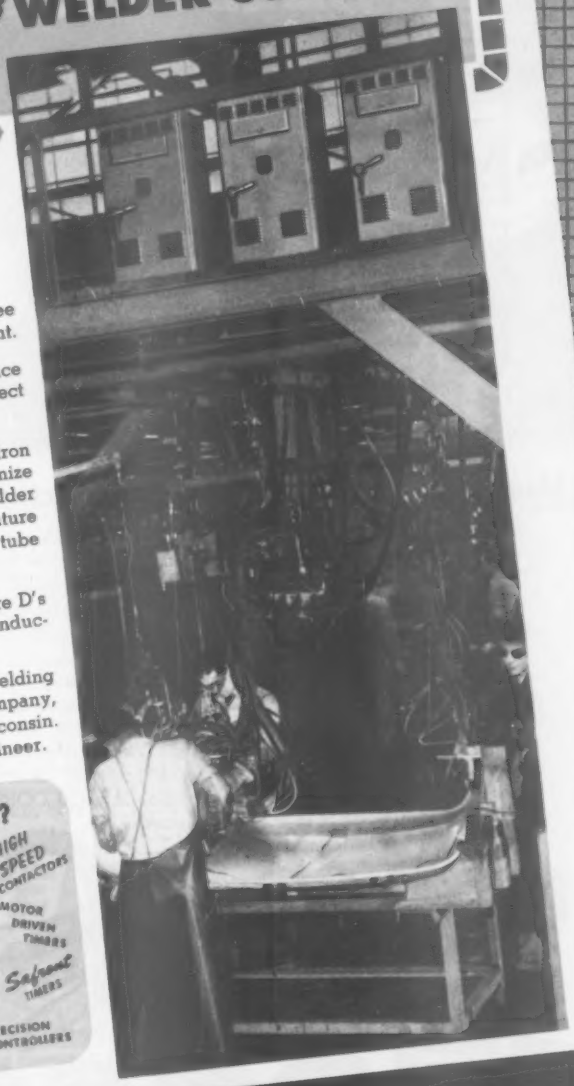
This equipment, which attaches to the forming and butt-welding machine, costs less than $2000 installed. It stops and starts with the welding and forming equipment and is positive in application.

The speed of replacing the coating is set to conform with the forming and welding equipment. The average is 100 fpm, but this can be greatly increased or decreased to correspond to manufacturing speeds.

Although the above covers only round tubing and containers, the manufacturer may find it adaptable to many other ap-



LOWER PRODUCTION COST WITH SQUARE D *Electronic* WELDER CONTROL



New Cleveland Automotive Plant Uses Square D Control on Gun Welders

Square D control is paying off in three important ways in this new mass-production plant.

LESS INSTALLATION TIME was needed since the contactor, sequence-weld timer and disconnect are combined in one enclosure.

LESS MAINTENANCE because firing of ignitron tubes is synchronized with power line to minimize transients and reduce strain on cables and welder transformer windings. Fail-safe weld time feature eliminates possibility of burning work because tube failure stops weld immediately.

MORE CONSISTENT WELDS because Square D's lead-trail circuit feature insures full-cycle conduction and prevents transformer saturation.

Can Square D help you do a better welding job? Write for Bulletin 8992. Square D Company, 4041 N. Richards Street, Milwaukee 12, Wisconsin. Or talk to your nearby Square D Field Engineer.

The BEST Resistance Welder Control?

There is no one type. Square D builds both magnetic and electronic controls of many types—each best suited to certain types of applications. Specify Square D, and be sure of getting the best for your job.

HIGH SPEED CONTACTORS

MOTOR DRIVEN TIMERS

Safeset TIMERS

Electronic TIMERS & CONTACTORS

SYNCRO-BREAK CONTACTORS

SYNCHRONOUS-PRECISION COMBINATION CONTROLLERS

SQUARE D COMPANY

Use ARCOS Low Hydrogen Electrodes

As a pioneer in the development of Low Hydrogen Electrodes, Arcos offers you the most complete selection available today. Whether your job involves welding mild steel, high strength-low alloy steels, sulfur-bearing free machining steels, chrome-moly steels, or low nickel alloy steels . . . you can be sure of the results you want.

Since 1942 Arcos has been turning out Low Hydrogen Electrodes under Stainless "quality controls". That's your assurance of consistently sound, high-strength welds on every job. It means there's nothing experimental about Arcos Low Hydrogen Electrodes. They've already been "tested and approved" on armor welding as well as commercial applications. ARCOS CORPORATION • 1500 South 50th St., Philadelphia 43, Pa.

WELD WITH ARCOS

Specialists in Stainless, Low Hydrogen and Non-Ferrous Electrodes

plications where it is desirable to fabricate from galvanized material.

For additional information on this new process, write to the Metallizing Company of America 3520 W. Carroll Ave., Chicago 24, Ill., specifying in detail material, gage, speed of welding, etc.

New Attachment Cuts 8 In. of Steel

A new cutting attachment that fits on either of two Prest-O-Weld welding blowpipes, cuts steel and other metals up to 8 in. thick. This is two to four times the thickness that previous models could cut.

The large increase in cutting range of the new attachment is achieved without a corresponding increase in operating pressures. Due to new design features, which include a large-diameter oxygen delivery tube and an improved mixer, only 55 psi oxygen pressure is needed to cut 4 in. of steel, and 100 psi will cut 8 in. of steel.

The new Type CW-122 cutting attachment was developed by Linde Air Products Co., a Division of Union Carbide and Carbon Corp. It connects to the W-121 and W-122 welding blowpipes as quickly and easily as a welding head. A hand-tightening connection nut locks attachment to handle. No wrench is needed.

A large selection of precisely fabricated cutting nozzles gives a wide range of usefulness to the CW-122, and assures smooth, efficient cuts on any work within its range. Twenty-three nozzles are available for general-purpose cutting, groove-cutting and many other special jobs.

Welding Rod and Solder

Chemalloy is a new development which revolutionizes and simplifies the art of welding or soldering any grade of aluminum as well as any zinc-base metal such as pot metal or white metal.

It requires no flux, no special cleaning or preparation of metal, no special welding equipment, no special skill. Merely heat metal beyond 800° F and rub on. It flows through the skin of aluminum and makes a weld stronger than the aluminum itself. It will, in turn, take soft solder and permit attachment of radio-electronic or electrical wires to aluminum. It is produced by blending several metals and chemicals to yield a very homogenous and finely grained metal. Melts and flows easily without splatter and free of lumps and irregularities.

Useful in every airplane, electronic, aluminum roofing, garage, foundry casting and automotive plant, or repair facility. Also ideal to repair damaged automobile radiator grilles, cracked carburetors, fuel pumps, automotive ornaments and accessories and aluminum utensils. Defies rust, corrosion, exposure and salt water.

Underwater Tube Testing Fixture

A new tube test fixture has been designed by the J. N. Fauver Co., Inc., Detroit, Mich., utilizing De-STA-Co Toggle Clamps. The fixture speeds the testing of tube shapes as used in the automotive and aircraft industry under air pressure. Air pressure applied to the tubes under water reveals leaks. A welded tank assembly, five De-STA-Co Toggle Clamps and an air pressure shut-off valve make up the fixture. Tube holding bars with rubber shut-off seals are bolted on the No. 228 clamps which hold the flexible tubing in place. The tubing is tested under normal plant line air pressure ranging from 40 to 160 lb. The No. 650 plunger type clamp raises and lowers the testing fixture in the tank.

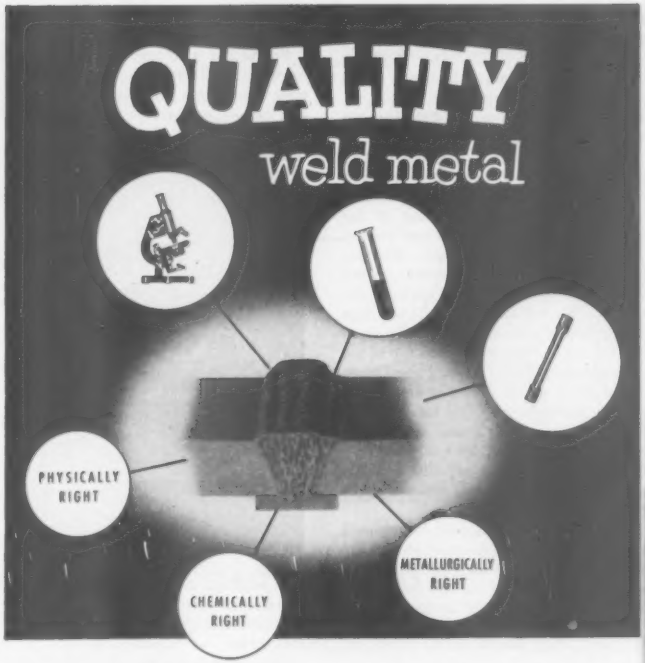
Positive holding pressure, ease of insertion and removal of the flexible tubing make De-STA-Co Toggle Clamps the logical choice for this unusual application. The De-STA-Co Toggle Clamps are manufactured by the Detroit Stamping Co., 322 Midland Ave., Detroit 3, Mich.

New Equipment

Several new developments in equipment and welding techniques were shown this year at the Lincoln Electric Co.'s Metal Show exhibit in Philadelphia.

Featured were new industrial models of a-c welding machines in 300, 400 and 500 amp capacity. The new units complete the Lincoln line of 60-, 180-, and 200-amp machines. The new design replaces previous 300- and 500-amp machines and incorporates new features as well as mechanical changes.

The new, more compact design has the same high output and overload capacity



Use ARCOS "Quality Controlled" Stainless Electrodes

The production of top-flight welds largely depends upon the inherent qualities of the weld metal. That's why Arcos Stainless Electrodes must pass so many "quality controls" in manufacture. It's your protection for soundness, specific mechanical or corrosion resistant properties, or microstructures that can stand up to destructive service conditions.

Whatever your welding job, you'll find it pays to put your confidence in Arcos. Backed by long experience with fabricators' welding problems, and research in the behavior of various grades of electrodes in use and weld metal in service—you can trust Arcos Stainless Electrodes to deliver consistently dependable welding results. ARCOS CORPORATION • 1500 South 50th St., Philadelphia 43, Pa.

WELD WITH

ARCOS

Specialists in Stainless, Low Hydrogen and Non-Ferrous Electrodes

as previous models and incorporates the patented "arc booster" circuit pioneered several years ago by Lincoln. The large easily read front mounted amperage dial is illuminated by an accessible 6-v lamp. A geared crank makes easy and smooth setting of amperage.

All electrical and mechanical control parts required for connecting or that need maintenance are accessible without removing the case. Reconnection of the input circuit for 220 or 440 v can be done without replacing any coils. An improved downdraft ventilation system brings air in from the top of the case, circulates it by baffles through the windings and prevents any air from being recirculated. The large lift bale on top of the case accommodates a standard crane hook.

Also there was exhibited as new equipment a hard-surfacing flux for hard surfacing with automatic hidden arc welding. With this flux a standard mild steel electrode is used, the hard-surfacing elements being contained in the flux and alloyed in the arc.

The adaptation of Manual Lincolnweld equipment for semiautomatic hidden arc welding to a gasoline-engine driven welder was also a feature of the Lincoln exhibit. This adaptation is a new addition to Lincoln's line of gasoline engine-driven welders, a 600-amp capacity unit. The new equipment will be useful on outdoor work such as pipe-line welding.

A new technique of automatic welding that has already been proved in a limited number of industrial applications was also demonstrated. This is the twin-arc process of hidden arc welding. It involves a unique and simple method of feeding two electrodes in tandem to deposit metal into a single crater. Speeds up to double those normally obtained with automatic welding have been successfully applied to several jobs, notably railroad car frame building and tank construction.

Backing Tape

Production is literally "blazing" along for Farrar & Trefts, Inc., Buffalo, N. Y., since they began burning pressure-sensitive tape as part of their welding method.

In addition, the new tape use "has resulted in substantial savings by eliminating machine stoppage and repairs of 'burn-thru' holes," the firm stated.

Used for submerged-arc welding jobs, the tape—either "Scotch" brand pressure-sensitive tapes No. 710 or No. 365—holds flux in place while the initial "stringer bead" weld of seams on metal tanks and flues is being made. It is being used successfully on metals from $^1/_4$" to 3-in. thick.

Applied to the underside of seam openings, the tape burns off as soon as the metal becomes red hot. The burning, it was pointed out, does not occur until a secure bond has been made.

For welding operations up to 700 amp, the firm uses the No. 710 tape (acetate backed), while for welds requiring up to 900 amp, the No. 365 tape (glass cloth backed) is used.

Both tapes are available through mill supply and paper jobbers nationally, according to the producer, Minnesota Mining and Manufacturing Co., St. Paul 6, Minn.

Copper-Clad Steel

Commercial production of copper-clad steel plate, a new engineering material combining the special properties of copper and steel, has been announced by Lukens Steel Co. Copper-clad steels are expected to meet a long-felt want for many electrical, heat transfer and corrosion applications requiring copper's high conductivity and corrosion resistance combined with steel's low cost, strength and rigidity Applications will include electrical apparatus, evaporators, hot water heaters, heat exchangers, tube sheets, tanks and vessels.

Combining unique chemical and physical characteristics with good fabrication properties, copper-clad steels simplify problems of equipment and fabrication in many types of applications. Fabrication time can be cut considerably, cutting production costs. For many types of equipment a smaller thickness of copper-clad steel can serve the same purpose as a thicker section of solid copper, reducing material costs and saving space and weight.

Available in plate gages only, Lukens Copper-Clad Steel consists of a layer of copper permanently and uniformly bonded to one side of a carbon steel backing plate. Cladding percentages of 10, 15 or 20% of total plate thickness will be regularly furnished in plate sizes up to 120 in. width, 380 in. long and $^9/_{16}$ to $1^1/_4$ in. thick. Copper-clad steels are also available in flanged only or flanged and dished heads.

Homogeneous and dense in structure, the copper is unchanged in any way by being bonded to the steel. Characteristic special corrosion resistance and excellent electrical conductivity are assured. Two types of cladding are regularly furnished: oxygen-free high conductivity copper for special electrical applications, and phosphorous deoxidized copper for improved weldability. Other copper and copper alloys can also be produced in the form of clad steels.

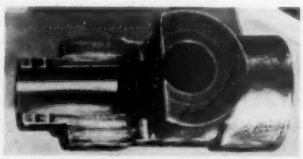
Mechanical properties of copper-clad steel are governed by the type of backing steel used. Backing steel may be any of the plain carbon or low-alloy steels of flange or firebox quality conforming to ASTM or ASME specifications.

Lukens Copper-Clad Steels can be sheared, formed or rolled according to standard procedures. Uniformity of cladding thickness facilitates fit-up and aids welding.

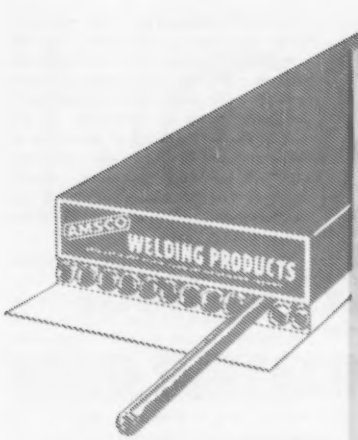
Silver Brazing Materials

New silver brazing materials which eliminate the major drawbacks of conventional silver brazing practices are now in production at the plant of the American Silver Co., 36-07 Prince St., Flushing 54, N. Y.

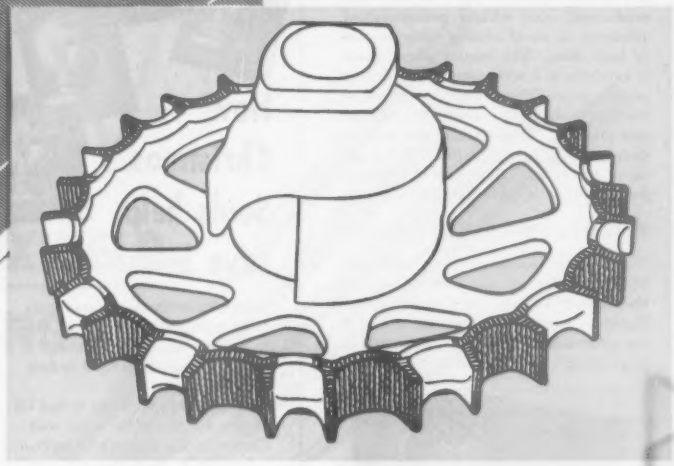
Called "Braze-Clad" metals, they permit the achievement of flawless blind joints and joints of long cross section without reliance on the skill of the operator.



Cut-away section of a high-pressure hydrogen line, showing a flawless joint of record length made with a "Braze-Clad" metal



How AMSCOATING with Amsco Hardfacing Rods saves you materials . . . manpower . . . money.



These teeth take bites out of repair costs!

AMSCOATING can make old Tractor Drive Sprockets last 3 times longer than new.

Here's another way to beat the high cost of replacements, down-time and maintenance . . . AMSCOATING the teeth on Tractor Drive Sprockets. Time after time service records have shown that AMSCOATING of sprockets results in these dollar-saving advantages:

1. ***On an average — 3 times the service life of a new, unhardfaced sprocket.***
2. ***2 out of 3 sprocket changes completely eliminated . . . for less down-time, less maintenance . . . more equipment out of the shop and on the job.***

To top it off, sprocket replacements can practically be eliminated. An AMSCOATED sprocket can be reclaimed over and over again . . . *at about half the cost of a new one!*

Get all the facts on how to make these big savings . . . write today for instruction sheet on AMSCOATING sprockets, and the name of your nearest AMSCO Distributor.

AMSCOATING...stands for control of wear by Hardfacing...

Hardfacing rods—and recommendations for their use—are as sound as the manufacturer who makes them. AMSCO has been fighting wear for a half-century—first with Manganese Steel, and later with AMSCO Hardfacing Products.

If you have a problem of wear caused by impact, abrasion, heat or corrosion . . .

Find out how AMSCOATING can save you materials . . . manpower . . . money!

AMSCOATING
THE RIGHT WAY TO SAY HARDFACING

AMERICAN Brake Shoe COMPANY

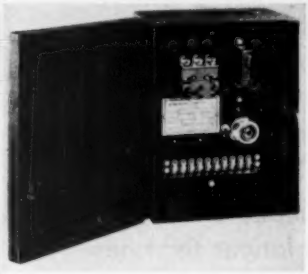
AMERICAN MANGANESE STEEL DIVISION
399 EAST 14th STREET • CHICAGO HEIGHTS, ILL.

Other Plants: New Castle, Del., Denver, Oakland, Cal., Los Angeles, St. Louis. In Canada: Joliette Steel Division, Joliette, Que.
Amsco Welding Products distributed in Canada by Canadian Liquid Air Co., Ltd.

"Braze-Clad" metals are combinations of various base metals (ferrous and nonferrous) clad with a predetermined thickness of silver brazing alloy on one or both sides. The brazing alloy (which is available in a wide selection of melting points) is prediffused into the surface of the base metal by the application of heat and pressure. "Braze-Clad" metals were developed by American Silver Co. engineers to over come the causes of failure of joints brazed in the conventional way.

Robotron Offers "Water Saver"

An added protection for resistance welding equipment has been offered by the Robotron Corp. of Detroit, Mich. Robotron engineers realized the necessity for automatic control of the water supply to a welder. As a result of their endeavors



they have developed the "Water Saver" which is a small, compact, easy-to-install unit.

The device's primary purpose is to automatically protect the welding transformer during operation and when idle, to protect it from condensation and eventual electrical failure. It saves on water flow, reduces sweating, rust, corrosion and mineral deposits.

When the welding machine initiating switch is closed and the sequence of the welder is started, the "Water Saver" relay is immediately energized. This closes the electrical circuit to the water valve solenoid, and water starts flowing. It completes the circuit to the heating elements in the flow switch, and then closes the ignitor circuit so that the Ignitron Contactor may be initiated.

As long as the welding sequence is repeated within the interval of one and one-half minutes, all of the above circuits remain closed. When the welding is stopped for a longer time than one and one-half minutes, all of the above circuits open and the water stops flowing. It allows stored heat in the welding parts to be dissipated before the water is turned off. "Water Saver" can also be used to cut down the needless flow of gas used in welding applications.

Robotron Corp., 21300 West Eight Mile Road, Detroit 19, Mich., will be glad to forward "Electronic Application and Engineering Data" on request.

How Christmas Seals help save lives

Successful methods of treatment make it more important than ever to find the 150,000 "unknown" cases of tuberculosis—and to find them early.

Mass X-ray campaigns to find TB in time are part of the work your Christmas Seal dollars help support.

Remember, no one can be "cured" until treated . . . and no one can be treated until the disease is discovered.

Send in your contribution today.

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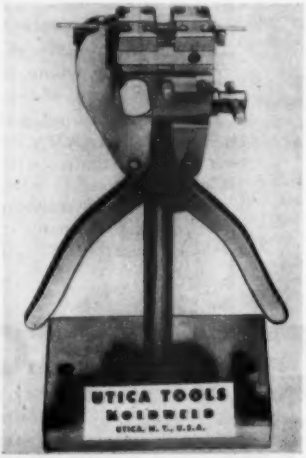
Because of the importance of this message, space contributed by **WELDING JOURNAL**

Koldweld Tools

Utica Drop Forge & Tool Corp., well-known manufacturer of quality hand



Utica hand tool for butt welding wires up to 0.070 in. diameter

The wire ends are placed in position on top on either side through holes provided. The knob on the right is then turned, which gives the wires the correct position and prepares them for the weld. The handles are squeezed and released. This completes the weld and the wires are removed.

tools, in cooperation with the Koldweld Corp., announce a remarkable, new device for the welding of nonferrous metals such as aluminum and copper without heat, flame or electrical current and without acids, fluxes or chemicals. In the case of the butt welding of wires it is as simple as inserting the two wire ends into a small device and squeezing plierlike handles. The job is accomplished in this way in a matter of seconds and no skill is required.

The process, originating in the laboratories of the General Electric Co., Ltd., of England (no connection with GE in the USA), is covered by some 45 patents and applications owned by GBC, Ltd. Sole licensee in the USA is the Koldweld Corp., headed by William Dubilier, President, well-known inventor and manufacturer and a prominent figure in the development of radio and electronics for many years. Arrangements have been made between Utica Drop Forge & Tool Corp. and Koldweld for the former to supply equipment for Koldwelding to manufacturers and others in the U. S. and abroad.

Due to the critical shortage of copper and the resulting interest in aluminum by the electrical, aviation, automotive and other industries, the Koldweld process has great significance. In the butt welding of strands of aluminum wire, for example, the weld is made in a matter of seconds. There is an actual intermolecular flow which accomplishes a small, compact bond said to have a greater tensile strength than the wire itself.

The Koldweld is such that after removal of the small flash of metal at the bond, the connection is the same gage as the wire itself, a great asset where compactness is essential, as in the manufacture of aluminum stranded cable. Where such cables are used for power transmission, this method of connection becomes very important. The clamps used at present have a tendency to loosen up, causing radio interference and disintegration at the joints.

In addition to use for the butt welding of wire, the Koldweld process can also be used in joining flat sheets of nonferrous metals. The bond is made solely by pressure, as in the case of the butt welding of wire. The bond is said to be so strong that, under test, the metal itself will tear out before the weld fails.

Utica Drop Forge & Tool Corp. states that two devices for accomplishing the Koldweld will soon be in production and available to industry. These are the Utica Koldweld Tool for Wire and the Utica Koldweld Tool for Sheet. They are compact, easily portable, weigh but a few pounds and operate basically on the pliers principle. Plans are now in hand to make a small pocket tool no longer than a standard pair of pliers for the electrical contractor and the home owner.

NOW!

LOOK TO REXWELD HARD SURFACING ROD

IT'S BETTER THESE WAYS:

CUT WELDING TIME

1. Rexweld builds up and holds on an edge better! This means less welding time is required.

INCREASE PRODUCTION

2. Rexweld flows more readily on flat surfaces! In machine gas welding operations valve companies have reported an increase in production per shift using Rexweld.

LESS POROSITY TENDENCY

3. Rexweld has less tendency for porosity because of its smoother flowing and better wetting properties.

BETTER WEARING QUALITY

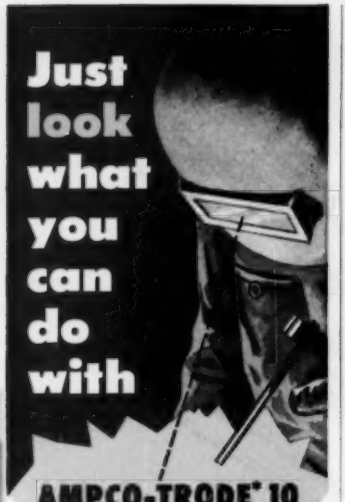
4. Tests show Rexweld has better wearing qualities.

Hundreds of satisfied users, manufacturing such products as valves, oil pump parts, mixer shafts attest to Rexweld's ability to do a better job . . . at less cost! Rexweld is available in a wide range of grades to meet all requirements. Then too, Rexweld meets Government specifications. If you use hard surfacing rod, be sure you check Rexweld . . . it'll save you time and money.

CRUCIBLE **REXWELD HARD SURFACING ROD**

52 years of Fine steelmaking

CRUCIBLE STEEL COMPANY OF AMERICA, GENERAL SALES OFFICES, OLIVER BUILDING, PITTSBURGH, PA.



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Bronze Electrodes

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Yes, you can do a lot with Ampco-Trode 10. That's why we call this covered aluminum bronze eloctrode a general-purpose rod.

When making overlays or when bronze-welding ferrous metals, we recommend medium arc and weave beads. Amperage range is wide, because it welds a wide variety of metals. Use low side of amperage range for joining iron-base metals . . . the middle of the range for bronzes . . . the high side of the range when welding bearing copper base alloys.

The versatile utility of Ampco-Trode 10 keeps costs down, so it pays to have a supply on hand. If you want additional information call your Ampco distributor or write us.

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IT'S PRODUCTION-WISE TO AMPCO-IZE!
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PERSONNEL

T. H. Wickenden Elected Member of Welding Research Council

T. H. Wickenden, Vice-President in Charge of Development and Research of The International Nickel Co., Inc., has been elected a member of the Welding Research Council of the Engineering Foundation for a three-year term beginning October 1st.

A. F. Davis Undergraduate Welding Prize Winners

David A. Thomas

David A. Thomas, joint author of the paper entitled "Metal Transfer in Arc Welding" which won first prize, is at present a fifth-year student in the five-year course in Metallurgical Engineering at Cornell University. Mr. Thomas was born in Baltimore, Md., and was graduated

in 1948 from the Arsenal Technical High School in Indianapolis. He entered Cornell University in the fall of 1948 and is a holder of a McMullen Regional Scholarship. During the summers Mr. Thomas has worked at the National Malleable and Steel Castings Co., Indianapolis Division, P. R. Mallory & Co., and Battelle Memorial Institute.

Paul J. Wisniewski

Mr. Wisniewski one of the joint authors of the paper entitled "Metal Transfer in Arc Welding" which won first prize in the A. F. Davis Undergraduate Welding Award was born Dec. 9, 1927, in Lawrence Mass. He graduated from Lawrence High School in 1935 and after attending Colby College for one semester served in the United States Coast Guard for

two years. Mr. Wisniewski is at present a fifth-year student in the school of Chemical and Metallurgical Engineering at Cornell University. He expects to receive his degree in June 1953. He is a student member of the AIME.

OBITUARY

Charles T. Posey

Charles T. Posey, of 310 Rolling Rd., Springfield, Pa., a Welding Engineer for a number of years with the Arcos Corp., Philadelphia, died Monday, August 25th, in the Byrn Mawr Hospital. He was 38.

Mr. Posey was a graduate of Vanderbilt University and was formerly employed by the Crane Co., Chicago. He was a member of the AMERICAN WELDING SOCIETY and the American Society for Metals. He also belonged to the Masons.

He is survived by his wife, Vivian Shank Posey, and two daughters Sherry Lynne and Janet, and by a brother, Dr. Thomas H. Posey, of Dayton, Ohio.

SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Schedule of Meetings

Bethlehem, Pa.—The following is the program of meetings of the *Lehigh Valley* Section for the remainder of the season. All meetings will be held at 8:00 P.M. at the Hotel Bethlehem, preceded by dinner at 6:30 P.M.

December 1st—"Tooling for Automatic Arc Welding," Cecil C. Peck, AWS Cecil C. Peck Co.

January 5th—"Battle of the Cities." Teams from Allentown, Bethlehem and Easton.

February 2nd—"Cement Mill Maintenance Panel." Three representatives from different cement plants will discuss the role arc welding plays in hard facing, fabrication and repair.

March 2nd—Joint Meeting of AWS and ASCE. "Adventures in Rigid Frame Design," Martin P. Korn, AWS, Consulting Engineer.

April 6th—"Application and Control of Welding," Roger W. Clark, AWS, Welding Engineer, General Electric Co.

May 4th—Annual Meeting and Ladies Night.

Electrode Coatings

Boston, Mass.—The November 10th meeting of the *Boston* Section will be addressed by H. F. Reid AWS, Metallurgical Engineer and Head of Electrode Technical Service Division, McKay Co., Pittsburgh, Pa. Mr. Reid's subject will be "Electrode Coating for Stainless and Mild Steel."

Nickel and Nickel Alloys

Buffalo, N. Y.—Speaking before 80 members and guests at the September meeting of the *Niagara Frontier* Section, Kenneth M. Spicer AWS, of the International Nickel Co. described techniques used in the fabrication and maintenance welding of nickel, high-nickel alloys and cast iron.

Regarding the nickel and high-nickel alloys, Mr. Spicer pointed out that regardless of the process used, the material must be thoroughly cleaned before welding is attempted. About 85% of the welding problems which have come to his attention have been solved by recommending cleaning procedures for use prior to welding. Lead and sulfur are particularly detrimental, either on the material or in the atmosphere, as they cause embrittlement in the weld. Also, welding slag should be thoroughly removed to avoid the possibility of corrosive attack by slag constituents.

Niagara Frontier Meeting
Left to Right, Robert P. Gehring, Chairman, K. M. Spicer, Speaker and I. Morrison, National Membership Chairman

In order to obtain complete penetration in furion welding these alloys, thinner root faces and wider bevel angles are used than would be required for similar welds in steel. In overlay work, low dilution with the base metal is desired, and low currents and slow travel speeds are used.

Filler wire is available for oxy-acetylene welding, submerged arc welding and inert gas welding of these materials, and Mr. Spicer emphasized that the filler wire is not interchangeable from one process to another because of the different metallurgical requirements.

In welding cast iron, it may be necessary to degas the casting to obtain sound weld deposits. This may be done by heating to a suitable temperature or by depositing one or two weld beads and then chipping them out and rewelding. Since the hardness of the base metal next to the weld depends on the cooling rate, preheating and postheating may be required to avoid cracking and hard spots. In repairing castings, if it is not possible to preheat the entire casting, the preheat should be applied in areas selected so that the crack will tend to open further. This will result in equalized stresses when the weld is made and the casting is cooled.

Mr. Spicer described a number of special techniques which have been used in particular applications involving these materials. The talk was well illustrated with slides.

Prior to the technical meeting, the dinner guests enjoyed a sound movie entitled "Telegram for America," which was shown through the courtesy of the Western Union Co. In the afternoon, 35 members visited the plant of Buflovak Equipment Division of the Blaw-Knox Co. for a two-hour tour.

Program of Meetings

Chicago, Ill.—Meetings of the *Chicago* Section as listed below will be held in People's Gas Auditorium, 122 S. Michigan Ave., except the December, February and May meetings which will be held at the Chicago Engineers Club. The meetings will start promptly at 7:30 P.M.

November 21st—"Welding Stainless Steel," George E. Linnert, AWS, Armco Steel Corp.

December 12th—"Oxygen Magic," R. L. Thuerbach, National Cylinder Gas Co.

January 16th—"Induction Heating Applications," T. J. O'Brien AWS, Weltronic Induction Heating Corp.

February 20th—"Hard Surfacing by Fusion Welding," H. S. Avery AWS, American Brake Shoe Co.

March 20th—"Welding Research: What It Is Doing and Where It Is Leading Us," Dr. A. Muller, Air Reduction Co.

April 17th—"Arc Welding Machine Design and Application," Raymond Freeman AWS, General Electric Co.

May 15th—"Arc Welding Electrodes—Composition, Function of Coating Ingredients, Slags," J. J. Chyle AWS, A. O. Smith Corp.

June 13th—Golf Outing. Details later.

Welding Arc in Engineering

Chicago, Ill.—President Charles H. Jennings was the guest speaker at the September 12th meeting of the *Chicago* Section held in the auditorium at the People's Gas Light and Coke Co. A general get-together and dinner was held at Burke's Grill and Restaurant, in order to give section members an opportunity to meet Mr. Jennings and the Second Vice-President, Eric Seabloom, also of the Chicago section.

Mr. Jennings is Engineering Manager of the Welding Department of the Westinghouse Electric Corp., and as was proved, he is well qualified to speak on the subject he chose "The Welding Arc in Engineering." He also showed a movie, taken at high speed, of the electric arc.

Sigma Welding

Cincinnati, Ohio.—C. A. Heffernon AWS of the Linde Air Products Co. was the guest speaker at the September 23rd meeting of the *Cincinnati* Section held at Engineering Society Headquarters. Mr. Heffernon discussed sigma welding.

Election of Officers

Columbus, Ohio.—The *Columbus* Section has elected the following officers for the 1952–53 season:

Chairman—Howard B. Cary, Marion Power Shovel Co., Marion, Ohio.
First Vice-Chairman—Melvin C. Clapp, 310 W. 7th Ave.
Secretary—Arthur P. Bruce, 2490 Renwood Place.
Treasurer—Joseph E. Brockmeyer, Surface Combustion Corp.
Chairman, Program Committee—Melvin C. Clapp, 310 W. 7th Ave.
Technical Representative—William Lee Green, Department of Welding Engineering, Ohio State University.

Welding Techniques

Dallas, Tex.—The September 19th meeting of the *Dallas* Section was addressed by Robert T. Howard [WS] of the Kansas City Division of the Bendix Aviation Corp. Mr. Howard's technical talk was on the subject "Nature of Heat in the Welding Process."

Program

Dayton, Ohio.—The following is the program of events for the *Dayton* Section for the coming year:

November 11th—"Warm-Up" Dayton Section Competitive Quiz Team, Team Captain, Major L. Paul Marking [WS], Engineers Club, 8:00 P.M.
December 9th—"Nondestructive Testing," S. A. Wenk, Battelle Memorial Inst., Columbus Ohio, Engineers Club, 8:00 P.M.
January 13th—"Welding of Corrosive Resistant Castings," W. E. Dundon [WS], The Duriron Co., Dayton, Ohio, Engineers Club, 8:00 P.M.
February 10th—"Welding Machine Development," C. H. Jennings [WS], Westinghouse Electric Corp., Buffalo, N. Y., Engineers Club, 8:00 P.M.
March 10th—Principles of Controls for Resistance Welders, Sponsored by Weldtronic, Engineers Club, 8:00 P.M.
April 14th—Safety Procedures in Welding, Sponsored by The Frigidaire Corp., Engineers Club, 8:00 P.M.
May 12th—Plant Visitation, International Harvester Co., Motor Truck Division, Springfield, Ohio.
June 9th—Spring Picnic (restricted to members only).

Corrosion

Denver, Colo.—An excellent technical talk on "Deterioration of Corrosion of Metals" was given by Frank LaQue of the International Nickel Co. at the September 16th dinner meeting of the *Colorado* Section held in the Cathedral Room of the Albany Hotel. Mr. LaQue very ably described in detail the act of corrosion as well as methods for correction. A motion picture film entitled "Corrosion in Action" was shown through the courtesy of the International Nickel Co.

This was a joint meeting with the Reclamation Technical Club, American Society of Mechanical Engineers and American Society for Metals. This was an outstanding meeting and very well received.

Silver Brazing

Des Moines, Iowa.—The *Iowa* Section held its first monthly dinner meeting of the fall season on September 25th at the Younkers Tea Room. Fifty members and guests were present at the dinner and a few more attended the meeting which followed. R. V. Metzler [WS] of the Engineering Division of Handy & Harman of New York was the guest speaker. Mr. Metzler presented an excellent extemporaneous talk on "Design for Silver Brazing," supplemented by a colored film entitled "Production Brazing with Low-Temperature Alloys." His talk covered the influencing factors in the design for silver brazing with comments on new methods, brazing alloys, applications and processes. The importance of cleaning, good fit, proper fluxing, assembling and supporting, and the application of heat were clearly emphasized. At the close of the talk there was an active question-and-answer discussion of the various phases of the talk.

Plant Tour

Detroit, Mich.—A plant tour of the American Blower Corp., Dearborn, was made by the members of the *Detroit* Section on Friday, September 12th. The members were welcomed by H. E. Paetz, City Sales Manager, who was responsible for conducting small groups through the plant and who was assisted by H. C. Rollert of the Industrial Relations Department. The American Blower Corp. are pioneers in sheet metal fabrication, the various types of welding necessary in their manufacturing operations were of interest to the members.

Low-Hydrogen Electrodes

Erie, Pa.—James E. Norcross [WS] of Arcos Corp. was the guest speaker at the September 9th meeting of the *Northwestern Pa.* Section held in the General Electric Community Center. Mr. Norcross covered new facts, methods and developments in his very interesting presentation on "Low-Hydrogen Electrodes and Their Characteristics and Use." Mr. Norcross has had vast experience in this field. The Arcos Corp. are pioneers in the development of low-hydrogen electrodes.

Newly Elected Officers

Erie, Pa.—The *Northwestern Pa.* Section have elected the following officers for the coming season:

Chairman—Fred T. Perry, 326 W. 31st St.
First Vice-Chairman—Jack H. Raimy, Welders Supply Co.
Secretary-Treasurer—Wilfred R. Boyd, Boyd Welding Co.
Asst. Secretary—J. S. Lentz, Boyd Welding Co.
Chairman, Membership Committee—Robert J. Huston, Jr., Air Reduction Sales Co.
Chairman, Program Committee—Jack H. Raimy, Welders Supply Co.
Technical Representative—Robert K. Allen, 2206 Eastern Ave., Wesleyville, Pa.

Sigma Welding

Fort Wayne, Ind.—The *Anthony Wayne* Section held its first monthly meeting at the Howard Johnson Restaurant on Sept. 19, 1952. A Swiss Steak Dinner was served to 45 guests and members and a vote of thanks was given the restaurant for the fine service and food.

R. F. McNutt [WS] introduced R. J. Frick who gave a description of the shielded-inert gas-metal-arc process. In his talk he showed the type of equipment used in sigma welding. This process was compared with other welding processes and the advantage of using sigma on aluminum, stainless and mild steel was discussed. The way Mr. Frick described the transfer of metal in the arc made everyone understand the process.

The meeting then moved to the Fab-Weld, Inc., plant where a demonstration was made on aluminum, stainless and steel plates. This meeting was very interesting and well presented. There were 60 members and guests at the demonstrations.

Welding White Metal

Grand Rapids, Mich.—The first fall meeting of the *Western Michigan* Section was held on Monday evening, September 22nd, at the Elks Cafeteria. Present for the dinner were 46 members and guests. They were joined by four more for the entertainment and technical session.

For entertainment the Section showed a film provided by the Marquette Manufacturing Co., titled "Marquette Goes to the Indianapolis Races." The film described the Marquette Co. as official welders at the track and the preparation of the cars for the race was very interesting.

The technical paper was given by R. H. Ogden [WS], President and General Manager of Aladdin Rod Mfg. Co. of Grand Rapids, Mich., whose subject was "Welding of Zinc Base Metals and Brazing Alu-

minum Without Flux." The 20-minute paper was followed by a demonstration on both zinc base metals and aluminum. Much interest was shown as several Companies are engaged in aluminum and zinc work in this territory.

The membership committee has been very active. Two new members were reported and another joined at the meeting.

Plans for the Season

Grand Rapids, Mich.—The Officers and Executive Committee of the *Western Michigan* Section held their August meeting at the Ionia Country Club in Ionia, Mich. Hosts were the Ionia Manufacturing Co. and Glen Hickok, Asst. Chief Engineer, who is also Vice-Chairman of the Western Michigan Section.

Several were present for golf in the afternoon and all of the Committee but two were present for the dinner and meeting.

The Committee has a well-rounded program planned for the Season, and with the increased activity of all the committees, especially the membership, larger crowds are expected at the meetings.

The high light of the August Committee meeting was the presentation of a complete set of golf clubs, a half dozen balls and a bag to the Secretary. The gift was very much appreciated.

Plans for the Year

Hartford, Conn.—The meetings of the *Hartford* Section for the coming year will be held at the Rockledge Country Club in West Hartford, Conn., on the second Thursday of each month. The Section is looking forward to a good season. The Program Committee is attempting to set up a program which will give the members practical technical knowledge on the subjects of inert arc welding and resistance welding.

Oil Industry Welding

Houston, Tex.—The *Houston* Section held its September 24th dinner meeting at the Ben Milam Hotel with an attendance of 122 at the dinner and the meeting which followed. John S. Cook [illegible] of the Humble Oil & Refining Co. presented an excellent extemporaneous talk on "The Control, Inspection and Testing of Welding Processes and Procedures." Before the meeting a motion picture film on the "High Lights of the Southwest Conference—1951" was shown through the courtesy of the Humble Oil & Refining Co. This was an excellent film.

Nontechnical Meeting

Kansas City, Mo.—The *Kansas City* Section initiated the new season September 11th with a dinner meeting at Fred Harvey's Pine Room. Thirty-two members and their guests were present at the 6:30 dinner.

Chairman Joe Payne called the meeting to order at 7:45. There were 40 persons in attendance.

Chairman Payne read a letter from Allen Wisler, District 6 Vice-President, concerning next year's district meeting. It has been arranged that this meeting be held in Houston on Monday, June 15, 1953, preceding the welding exposition. A vote was taken and these arrangements were agreed upon. It was also voted to send the Chairman, Secretary and Program Chairman of the Section to this meeting.

Program Chairman George Demecs introduced the first speaker of the evening, R. Rotsch of the Missouri State Conservation Commission. Mr. Rotsch gave an excellent talk on the conservation program as practiced in Missouri. He pointed out the fallacy in most people's thinking that the main function of the Commission was to control seasons, laws, limits, etc. He stated that the most important step in the conservation of wild life is to provide proper environment. The decrease in the amount of food, cover and water has caused the wild life population to decrease to its present level. Mr. Rotsch concluded his talk with a color sound film on the mating of the prairie chicken, spinytailed and sharp-tailed grouse.

Mr. Demecs then introduced F. O. Hudson of the Kansas City Water Department. Mr. Hudson presented a very interesting talk and color sound movie on the local water purification plan. He followed the process from the time the water was taken from the Missouri River until it was supplied to the consumer.

This nontechnical program was very well received and the Section plans on having similar speakers on subjects of general nature throughout the year, in addition to the regularly scheduled technical speakers.

The meeting was adjourned at 9:45.

Welding Panel

Los Angeles, Calif.—The Aircraft and Rocketry Welding Panel of the *Los Angeles* Section of the AWS continues to gain widespread interest in its activities. At its last meeting on September 4th in Swally's Cafe, after a panel dinner, 48 members attended discussions by Russell Meredith, North American Aviation, on Fusion Welding of High Strength Aluminum Alloys, and by G. D. Smith, G. D. Smith Co., on Ultrasonic Testing.

Mr. Meredith pointed out that unlike conventional aircraft, the present-day guided missiles are essentially flying fuel tanks. It is necessary that the missile skins be joined with pressure-tight joints and, due to the low and elevated flight temperatures, the present-day conventional riveting processes are inadequate as a joining method.

Seam welding can be used and is being used on North American's missiles, but, due to the size and inaccessibility of some of the components, seam welding cannot be entirely used for sealing.

For many years, industry has frowned upon the use of fusion welding of the high-strength aluminum alloys. Especially the alloys containing copper such as 24S-T.

This skepticism is shown because of the metallurgical phenomenon known as eutectic melting or copper precipitation from the grains into the grain boundaries which takes place during welding.

To justify the possibility of fusion welding 24S-T aluminum, a comparison was drawn between the procedures used in welding nonstabilized copper; it is generally known that the amount of intergranular corrosion in stainless is directly proportional to the time at temperature. Some eutectic melting will take place when seam welding high-strength aluminum alloys. The amount of copper precipitation in seam welds again depends on the speed of travel and heat input. Consequently, it is believed that high-strength aluminum alloys may be welded with satisfactory results by welding at very high speeds and chilled with hold-down bars and backing mandrels using full automatic inert-gas-shielded nonconsumable electrode and mechanized jigging fixtures.

Tests conducted by Cornell University, using a specially constructed multiarc torch, have shown that weld efficiencies of 75% of the parent metal (24S-T) may be obtained.

Tests conducted by the North American Aerophysics Division, using full automatic Heliarc welders, have shown weld efficiencies of 85% of the parent metal (0.032 and 0.062 24S-T3 aluminum alloy sheet).

G. D. Smith presented a very good extemporaneous lecture with the use of slides. The slides and verbal explanations pointed out the limitations and potential horizons for the use of nondestructive testing of stock material forgings, wrought metal, machined parts, etc., with a reflectoscope.

Basically, the reflectoscope consists of three essential parts. An electronic generator creates ultrasonic waves by supplying a high-frequency a-c voltage to a quartz crystal. The crystal radiates these waves into the material and also acts as a receiver and detects back reflections. An electronic amplifier applies the reflected signal to a cathode-ray oscilloscope, which measures the time interval between transmission of the outgoing and reception peaks, one representing the initial pulse or surface of the material and the other representing the reflection from the opposite side. A discontinuity will result in a "pip" occurring between the two peaks. By superimposing a calibrated square wave on

the screen a discontinuity may be located accurately.

There are two methods employed for inspection: reflection and through-transmission. The reflection method will locate internal discontinuities which present a reflecting surface to the sound beam as it passes through the material. The through-transmission method detects discontinuities associated with two physical properties of the material, a change in density or mass, and a change in modulus of elasticity or stiffness.

Most metals can be inspected by the reflection method. The main disadvantage is that the reflection method does not accurately detect flaws in materials with thicknesses less than one-half inch. Consequently, the present-day reflectoscope is not adequate for general inspection usage for aircraft and missiles.

Resistance Welding

Louisville, Ky.—E. A. Mallett [M2] of the Taylor Winfield Corp. was the speaker at the September 23rd meeting of the *Louisville* Section held at Kapfhammer's Party House. Buffet luncheon was served at 7:00 P.M.

Mr. Mallett is widely recognized both here and abroad for his knowledge not only of resistance welding machines, but also of the proper design of products to be resistance welded and the design of the dies, jigs and fixtures required to do the job consistently and with a minimum of down time. He combined his technical knowledge as a Design Engineer with years of practical experience gained as Welding Engineer in machinery and automotive plants and as Application Engineer for the Taylor Winfield Corp., one of the largest manufacturers of resistance welding machines. He is credited with many of the developments which have brought resistance welding machines out of the woods of "rule-of-thumb" designs to the present-day well-engineered and substantially constructed machines which can be depended on to be absolutely consistent in their operation. Mr. Mallett is recognized as one of the foremost authorities in the specialized field of resistance seam welding.

Milwaukee Program

Milwaukee, Wis.—Regular meeting dates for the remainder of the 1952–53 season of the *Milwaukee* Section are as follows:

Friday, November 14th—Speaker, J. R. Wirt; Subject, "Production Brazing."

Friday, December 12th—Speaker, John Mikulak; Subject, "Production Design for Welding."

Friday, January 23rd—Speaker, Richard K. Lee; Subject, "Recent Developments in Arc Welding Electrodes."

Friday, February 20th—Speaker, John W. Mortimer; Subject, "Inert Gas Welding."

Friday, March 27th —Speaker, Wm. C. Henglick; Subject, "Maintenance Welding."

Friday, April 24th—Speaker, J. J. Barry; Subject, "Hard Facing."

May 16th—May Party.

1953 Educational Course—Jan. 12, 19, 26 and Feb. 2, 1953.

Welding Metallurgy

Milwaukee, Wis.—The regular monthly dinner meeting of the *Milwaukee* Section was held on Friday, September 26th, at the Ambassador Hotel with an attendance of 100. After-dinner speaker was George Schrieber of the Sales Office, John Rouschenberger Co., who spoke on the topic "The Living Dead."

Technical speaker was Robert Aborn [M2], Assistant Director of Research Laboratory, U. S. Steel Co., Kearny, N. J. Mr. Aborn's subject was "Metallurgy of Ferrous Welding." He opened his talk with a brief consideration of metallurgy principles involved in welding, and then how to apply them to the making of welds. This included the factors controlling rate of heat in part and dissipation, why cooling rate is so important and how its influence will be established from transformation diagram of particular steel; then a discussion of metallurgy characteristics of weld metal and the desirable structure for some specific application. The welding of dissimilar ferrous metals was also briefly treated, and finally a brief look into the future of welding from the metallurgical viewpoint. The talk was supplemented with slides and a movie.

Annual Picnic

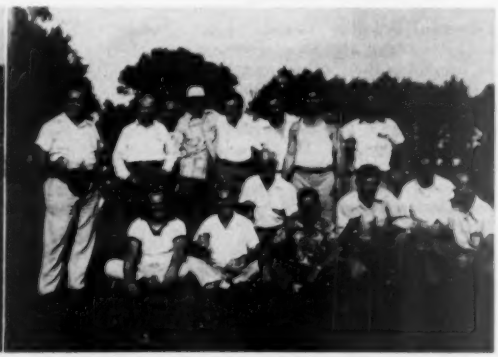
Newark, N. J.—The Annual Picnic of the *New Jersey* Section of the AWS was held at "The Pines" in Metuchen, N. J., on Sept. 20, 1952. This year all records were broken with regard to attendance. Approximately 450 members and guests enjoyed the outdoor activity under perfect sunshine. There were many types of contests in which anyone could participate. These included Softball, Horseshoe Pitching, Flycasting and Nail Driving. Needless to say all this was accompanied with adequate supplies of food and drink.

In accordance with the usual custom, everyone participated in the drawing for gifts with more than casual interest because of the large number and quality of the gifts.

Hard Surfacing

Newark, N. J.—The first meeting of the **New Jersey** Section of the AWS was held at the Essex House in Newark, N. J., on Tuesday evening, Sept. 16, 1952. Approximately 40 members and their guests attended the dinner which preceded the actual meeting.

Airco Soft Ball Team



Esso Bayway Welders Team (The Winners)

New Jersey Picnic

A nontechnical film, "Fighting Lady's Family," was shown. This depicted the part played by an aircraft carrier in modern warfare and was well received.

The technical portion of the meeting consisted of an excellent presentation by Howard S. Avery AWS the principal speaker. Mr. Avery's talk was titled, "The Application of Special Weld Metal Surfacing for Extreme Conditions of Service." Nearly 100 members were present for this portion of the evening's program which was informative and interesting because of the excellent manner in which Mr. Avery handled the subject. It is appropriate to note that Mr. Avery is one of the outstanding metallurgists in the field of hard surfacing and was therefore in a position to cover all facets of the subject that were of interest to the audience. Refreshments were served after the meeting.

Jigs and Fixtures

Pascagoula, Miss.—The regular monthly dinner meeting of the *Pascagoula* Section was held on September 10th at the Shipyard Cafe with an attendance of 57 at the dinner and meeting. A. K. Pandjiris AWS, President of the Pandjiris Weldment Co., St. Louis, Mo., was the guest speaker at the meeting. His subject, "Set the Pace, Win the Race," covered welding positioners and jigs—their application, uses and advantages. Mr. Pandjiris' excellent presentation was nontechnical and was of general interest to all. An after-meeting feature was the showing, through the courtesy of the Westinghouse Co., of a sound color film entitled "Energy is Our Business." This was an excellent meeting.

Pressure Vessels

Pascagoula, Miss.—N. J. Le Blanc, Chief Inspector, Louisiana State Anhydrous Ammonia Commission, Baton Rouge, La., read an excellent illustrated nontechnical paper on "Welding of Pressure Vessels" at the October 1st dinner meeting of the *Pascagoula* Section held at the Shipyard Cafe.

An after-meeting feature was the showing of a sound color film entitled "Enchanted Holiday" which was shown through the courtesy of the Moormack Steamship Co. This excellent film showed all-welded vessels in operation.

Toughness of Welds

Peoria, Ill.—The first dinner meeting of the season for the *Peoria* Section was held Wednesday evening, Sept. 17, 1952, at "The Mecca," 505 Farmington Rd., Peoria, Ill.

The speaker was Austin Hiller AWS, Welding Sales Engineer, General Electric Co., whose subject was "Toughness of Welds."

The talk was preceded by a movie, "Metal Magic." Mr. Hiller presented an extremely interesting discourse pointing out that toughness of welds is affected by many items, such as embrittlement, internal stress, gas, slag inclusions, welding techniques, base material, electrode, type of joint, etc. His talk was followed by a lively question-and-answer period of 25 minutes.

Welding Engineering Course

Philadelphia, Pa.—An evening course in the Principles of Welding Engineering which began on September 24th is being sponsored by the *Philadelphia* Section at the Drexel Evening College. This course has been given a good deal of study and planning by the Educational Committee of the Philadelphia Section with full approval and cooperation of the national officers of the AMERICAN WELDING SOCIETY.

David Thomas, Jr., Chairman of the Educational Committee, reports that the enrollment has exceeded 73, which is oversubscribed, and that arrangements have been made to enlarge the program. It is indeed gratifying to know that the responsible leaders of industry in the Philadelphia area are responding with great interest in this phase of the AWS activity.

Acting as evening College Coordinator for the AWS, and an active member of the Section, Harry Pfeffer is handling complete arrangements and facilities at Drexel Evening College, with the full approval and active support of Dean Kenneth Riddle, well-known Philadelphia educator at Drexel Institute of Technology. Instructors Raymond G. Bleil and Lorin K. Poole, both able men in the field of welding metallurgy and related applications, feel that local industry will benefit greatly from this educational program as it is of special interest to technicians, shop foremen, shop supervisors, design draftsmen, inspectors and welders.

Among the many subjects covered in this course will be metallurgy, design inspection, welding processes codes and specifications. Twenty-four weeks will be required to complete the course which is divided into two parts of 12 weeks each. It is generally thought that an educational program of this type has been badly needed for some advantages will be found for all phases of the metalworking industry.

Gas Pipe Lines

Phoenix, Ariz.—The *Arizona* Section of the AMERICAN WELDING SOCIETY held its first meeting of the new year in the Saratoga Room of the Hotel Westward Ho at 7:30 P.M., Wednesday, September 17th.

The Chairman, Ralph Smith, announced to the Section that Hal Savage of the Arizona Welding Equipment Co. asked to be relieved of his office of Program Chairman, due to pressure of business. His successor, by executive committee action, is John Dyer, General Manager of the Arizona Welding Equipment Co.

J. August Rau gave a short synopsis of his recent trip to Chicago where he attended the joint AMERICAN WELDING SOCIETY and American Society of Civil Engineers meeting on the latest welding practices.

A report was also given by John Dyer as to the success of the membership committee, of which he was formerly chairman before assuming program chairmanship. The Section was glad to hear of the new members resulting from the committee drive.

The speaker of the evening was Barry Hunsaker, Project Engineer of the El Paso Natural Gas Co. Mr. Hunsaker was accompanied by Fred T. Johnson, Chief Welding Engineer of El Paso Natural.

The gentlemen presented a paper, followed by a question-and-answer period which very capably described the development of welding and the allied processes in the company's pipe-line construction and also in the design and construction of compressor stations. It was quite apparent that El Paso Natural is a leader in these fields and the procedures followed are a result of sound engineering thinking and application. The Section is indeed grateful to these men for having traveled to Arizona from El Paso, Tex., to give their paper.

women over 35

While breast cancer is the commonest cancer among women of your age, it is also highly curable if treated properly *before* it has spread beyond a local area in the breast itself.

Doctors tell us that women are much more likely to be the first to discover the earliest signs of breast cancer. That is why you should know the correct and most thorough way to examine your breasts for any lumps or thickening.

For information as to where to see our new life-saving film "Breast Self-Examination" call us or write to "Cancer" care of your local Post Office.

American Cancer Society

Plant Tour

Pittsburgh, Pa.—Around 75 members of the *Pittsburgh* Section enjoyed a luncheon in the Dravo Corp. cafeteria at Neville Island, and took part in a tour of the Dravo Corp. plant. The tour took two hours and included all facilities including the launching of an all-welded steel barge. The Section is deeply grateful to the personnel of the Dravo Corp. who made this very interesting tour possible.

Schedule of Meetings

Richmond, Va.—The following is a program schedule for the remainder of 1952–53 season of the *Richmond* Section:

November—"The Metallurgical Changes Occurring with Welding," H. Kuenzel, Head Mechanical Engineer, University of Alabama.
December—"Preheating for Welding," A. B. Tesmen, Tempil Corp.
January—"Hard Facing to Minimize Industrial Wear," H. S. Avery, Research Metallurgist, American Brake Shoe Co.
February—"Cracking in Welding," J. Heuschkel, Manager, Welding Section, Westinghouse Electric Corp.
March—Lew Gilbert, Editor, *Industry & Welding.*
April—"Designing Welded Fabrication of Stainless Steel," G. E. Linnert, Research Welding Metallurgist, Armco Steel Corp.
May—Welding Quiz.
June—Annual Picnic.

Tool and Die Welding

Saginaw, Mich.—Patrick Doyen (MS), Sales Engineer, Welding Equipment & Supply Co., Detroit, Mich., spoke extemporaneously on "Tool and Die Welding" at the September 12th dinner meeting of the *Saginaw Valley* Section held in the High Life Inn. Seventy-four were present at dinner and 80 at the meeting. A demonstration accompanied Mr. Doyen's excellent semitechnical talk.

Tank Armor Plate

San Francisco, Calif.—Ninety-five members and guests attended the September 22nd dinner meeting of the *San Francisco* Section held at the Engineers Club. Coffee speaker was Capt. George Dyson of the Ordnance Department of the U. S. Army, Depot of Field Operations. Captain Dyson in his subject "Tanks and Guns" told some of his very interesting first-hand experiences gained in combat in Korea.

Technical speaker was Patrick J. Coyne, General Foreman, Maintenance Department of the U. S. Army, Benicia Arsenal. Mr. Coyne's subject, "Welding of Tank Armor Plate," covered procedures used to qualify welding rods. He explained how ballistic tests of welds are made; also a breakdown of the welding procedures followed to weld armor plate; how the Arsenal prepares usable portions of damaged tanks; and the practice of combining old and new parts to return tanks to combat service. Some of the methods used to prepare armor plate for welding, such as powder cutting, etc., were discussed.

Mr. Coyne has had over thirty-years' experience in the machine tool industry, 15 of these with our country's combat tank program from the earliest pilot models.

Rockets

Schenectady, N. Y.—The first meeting of the 1952–53 season of the AMERICAN WELDING SOCIETY took place on Thursday, Oct. 2, 1952. The meeting was held at Danish Hall, 989 Albany St., Schenectady, N. Y.

The speaker, Dr. Charles F. Green, Consulting Engineer, Aeronautic and Ordnance Systems Department, Schenectady Works, presented a talk entitled "Rockets into Space." Dr. Green traced the history of the development of rockets from the end of World War I to date. The talk was illustrated by both slides and motion pictures of rocket usage from the German V-2 to those rockets used at the White Sands Proving Grounds.

Dr. Charles F. Green, consulting engineer in charge of advanced development program of the Aeronautic and Ordnance Systems Divisions, was graduated from Kansas University in 1914 with an A.B. degree, and in 1915 received his master's degree. During World War I he served as a pilot in France and he received his Ph.D. from the University of Illinois in 1920. After teaching at the University of Illinois and Massachusetts Institute of Technology, he was a member of General Electric's retained staff from 1925 to 1929, when his connection with that company became a permanent one. Prior to his present appointment he served as consulting engineer in the Marine and Aeronautics Engineering Division.

As a technical representative with the Army Air Force during World War II, Dr. Green was a member of one of the teams of U. S. scientists that followed our troops into "buzz bomb" laboratories and launching sites to learn first hand of German developments in guided missiles.

The meeting was conducted by I. W. Johnson, Chairman of the *Northern New York* Section, AMERICAN WELDING SOCIETY. All new officers for the coming year were introduced. In addition, the past-chairman's pin was presented to W. J. Van Natten, last year's chairman.

Dr. Green's talk was in nontechnical terms and easy to understand.

Practical Weldesign

Shelton, Conn.—The September 18th dinner meeting of the *Bridgeport* Section was held at Rapp's Restaurant in Shelton. Coffee speaker was Donald Packer, Advertising Manager of Producto Machine Co. His topic was "Advertising for Small Business." Feature speaker at the technical meeting was Richard Lindgren of the Lincoln Electric Co., Cleveland, Ohio. Mr. Lindgren presented an excellent semitechnical talk on "Practical Weldesign." Mr. Lindgren is co-author of the *Weldesign Manual* and an instructor of current "Weldesign" courses being given by the Lincoln Electric Co. in this area.

Nondestructive Testing

Shelton, Conn.—Roy O. Schievel, Assistant District Manager of the Magnaflux Corp., New York, gave an illustrated talk on "Nondestructive Inspection of Welds" at the October 16th dinner meeting of the *Bridgeport* Section held at Rapp's Restaurant.

Coffee speaker was Robert Insinger of the Peabody Engineering Co. who spoke on "The Metal Industry in the Netherlands."

Welding Procedures

South Bend, Ind.—The *Michiana* Section is well under way again with a right smart discussion of the phases of qualifying welding procedures and operators given by its genial Second Vice-Chairman, Eric Seabloom, who is also Supervisor of Field Engineering for the Crane Co. in Chicago. Amply illustrated with slides, Mr. Seabloom's talk covered procedures required by boiler codes for various types of pressure vessels, and gave a very good picture of what is required to get certified.

And what a procedure! Root bends, face bends, tensiles, magnaflux, X-ray! If a man can weld these test bars satisfactorily, he's sure got the touch.

The above dinner meeting took place on September 25th at Nabicht Bros. Restaurant.

Inert Arc Welding

Syracuse, N. Y.—The new season for the *Syracuse* Section started at 6:30 P.M. in the Hiawatha Room of the Onondaga Hotel on Wednesday, September 10th. About forty members and guests had an excellent roast beef dinner. In line with this section's policy of developing their social and civic responsibility, the coffee speaker, F. T. Wood, described the organization and program at Dunbar Center (which is a very active social center for the underprivileged citizens in Syracuse).

The regular meeting started at 8:00 P.M. with a short description of how to use a spark test for material identification. After a general description of sparking, G. F. McCracken of Crucible Steel demonstrated how chemical composition influenced sparks (from a grinding wheel) with a series of typical shop materials.

Over 150 members and guests heard J. W. Cunningham of Air Reduction talk on "Inert-Gas-Shielded Arc Welding." New developments with the Aircomatic process were described and illustrated with a series of slides. A spirited question-and-answer period followed. The meeting adjourned a little after 10 o'clock to watch a fight on television and sip a little coffee.

Schedule of Meetings

Syracuse, N. Y.—The 1952–53 program of meetings of the *Syracuse* Section to be held in the Hiawatha Room of the Onondaga Hotel is given below. Dinner will be served at 6:30 P.M. The meeting will start at 8:00 P.M.

November 12th (New Member Night)—"Cracking in Welding," J. Heuschkel AWS, Research Engineer, Westinghouse Corp.

December 10th—"Resistance Welding," R. T. Gillette, Welding Consultant AWS, Precision Welder and Machine Co.

January 14th—Program to be announced.

February 11th—"Metallic Arc Welding with Low-Hydrogen Electrodes," D. L. Mathias, AWS, Research Engineer, Arcrods Corp.

March 11th—"Product Design for Welding," J. Mikulak AWS, Assistant to the Vice-President, Worthington Corp.

April 8th—"Recent Developments in Welded Bridges and Buildings," F. H. Dill AWS, Welding Engineer, American Bridge Co.

May—Date to be announced for the Annual Dinner Meeting.

Tour

Wellsville, N. Y.—The *Olean-Bradford* Section had a tour of the Worthington Pump Corp. plant in Wellsville on September 29th. Eighty members and guest participated. A fresh ham dinner was enjoyed at the Elks Club Rooms preceding the tour.

Wichita Program

Wichita, Kan.—A round-up of the various welding activities in the *Wichita* trade territory, with speakers from various manufacturers in and around Wichita, was the event for the September 8th dinner meeting held in the Gold Room of the Coleman Co., Inc. The speakers and the subjects they covered are as follows:

J. C. Woodin, Supt., Wichita Board of Education, talked on what is being done in the city schools about welding.

Paul Carter AWS, Boeing Airplane Co., talked about "Titanium."

C. F. Madden AWS, Beech Aircraft Co., talked about "Spot Welding."

A. F. Schroeder, Cardwell Manufacturing Co., talked about some of the problems they have encountered and conquered in the heavy welding industry.

Donn Boring, Tweco Products Co., talked about "Canada."

V. C. VanHorn AWS, Bert & Wetta & VanHorn, talked about the self-propelled alfalfa chopper (The Field Queen) which they have developed, and are building and selling all over the United States and Canada.

E. A. Bussard AWS, Coleman Co., Inc., talked about "Welding Standards."

Lee Ormsbee AWS, Watkins, Inc., talked about "Cutting Stainless Steel."

Meeting was considered exceptionally educational by those present.

Refinery Welding

Wichita, Kan.—Seventy-nine men attended the meeting of the *Wichita* Section on October 7th which began at 5:30 P.M. with a trip through the newly modernized refinery of Socony-Vacuum Oil Co. in Augusta.

Dinner at City Hall followed at 7 P.M. and the regular meeting was held afterward.

Speakers were: Robert L. Curry, Kanotex Refinery Co., Arkansas City, who spoke about their welding program for the last eighteen months, and about lining their tower units with Monel and stainless steel. W. O. Wade, Skelly Oil Co., Eldorado, spoke about building up valve slide castings and postheating of same. Leon E. Kirk, Vickers Petroleum Corp., Potwin, spoke about the first methods of welding tanks, and also about the lining of fractionating towers with stainless steel plate. Harvey Bates, Socony-Vacuum Oil Co. Augusta, spoke about building the impeller for their centrifugal pump from available materials, about alloy tubings, and about seal-welding rivets in riveted vessels.

The talks were followed by very interesting and informative questions and answers.

The meeting was exceptionally well attended and considered well above average by those present.

Election of Officers

York, Pa.—The *York - Central Pa.* Section announces the election of the following officers for the year 1952–53.

Chairman—Charles H. Pyle, 777 Grandview Rd., York, Pa.

First Vice-Chairman—Frank A. Logue, Alloy Rods Co.

Secretary—John Kugle, 1256 Prospect St.

Asst. Secretary—Miss Emily Light, 447 W. Market St.

Treasurer—Charles W. Allen, 132 W. Jackson St.

Chairman, Membership Committee—Gordon Appleby, 1233 E. Maple St.

Chairman, Program Committee—William T. De Long, The McKay Co.

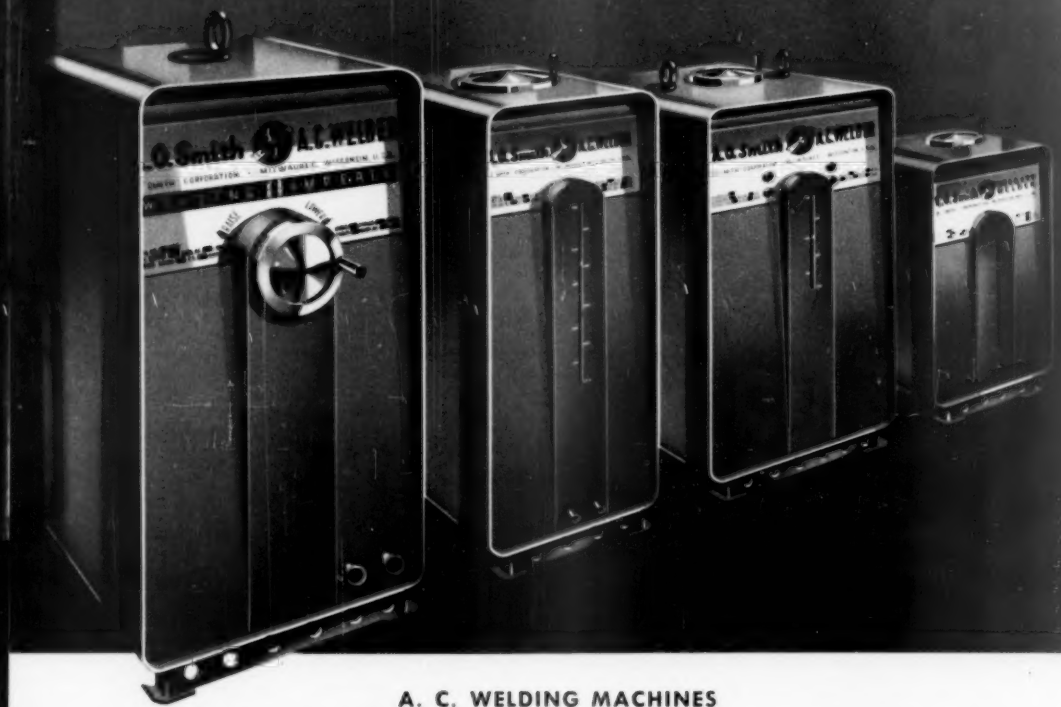
Technical Representative—Richard K. Lee, 521 Colonial Ave.

York Officers

York, Pa.—The accompanying photograph, taken at the August 18th meeting of the *York-Central Pa.* Section, shows some of the members of the Board of Directors. Reading from left to right, they are as follows: Wm. DeLong, Metallurgist, McKay Manufacturing Co.; Paul L. Stumpf, Subcontract Manager, S. Morgan Smith Co.; Marvin Silberger, Metallurgist, American Chain & Cable Co.; Chas. Allen, Methods Man, A. B. Farquhar Co., York; Chas. Pyle, Sales Engineer, Lukens Steel Co.; Donald McDivitt, Mechanical Engineer, Posey Iron Works; Paul Lang, Sales Engineer, Arcway Equipment Co.; and John Kugle, Engineer, A. B. Farquhar Co., York.

The Section also wishes to take this opportunity to thank the York Dispatch "for the fine courteous service rendered in publishing an account of the meeting and a picture of same in their newspaper."

new era in welding!



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Supplement to the Welding Journal, November, 1952

Temper Brittleness in Low-Alloy Steel Weld Metal

Temper-brittleness studies in five low-hydrogen low-alloy weld metal compositions and alternative heat treatments

by Richard P. Wentworth and Hallock C. Campbell

Abstract

The temper-brittleness phenomenon has been investigated in five low-hydrogen low-alloy weld metal compositions. A slow notch-bend test was developed in order to give a ductile-or-brittle (pass-or-fail) indication. By the use of two criteria—fracture appearance and percent contraction in width of specimen beneath the notch—the isothermal development of temper brittleness could be studied.

This test revealed temper brittleness in two Ni-Mo-V grades; it was less serious in the 100,000 psi grade than in the 120,000 psi grade. The range of embrittlement was 1050 to 1150° F in the 100,000 psi grade, as compared to 950 to 1250° F in the 120,000 psi grade. Long tempering times (over 10 hr) at 1200° F improved the ductility of the 120,000 psi grade, and considerably shorter times at 1250 and 1300° F also improved the ductility. Ductility was completely restored by holding at these higher temperatures for 2 to 4 hr.

It was also demonstrated that if the weld metal was initially rendered ductile by heating above the temper-brittle range, the ductility could be retained in spite of heating in the temper-brittle range (950 to 1150° F). A 1650° F austenitizing heat treatment also appeared to be beneficial for 120,000 psi Ni-Mo-V weld metal, as it narrowed the temper-brittle range.

Temper brittleness was not revealed in tests of three other low-hydrogen electrode grades. Two Mn–Mo grades (low and medium carbon) gave completely ductile fractures. Type 2Cr – $\frac{1}{2}$Mo weld metal, because of its high as-welded hardness, required tempering at 1100° F or above to achieve satisfactory ductility.

Richard P. Wentworth is Metallurgist and **Hallock C. Campbell** is Associate Director of Research and Engineering with the Arcos Corp., Philadelphia, Pa.

Paper was presented at the Thirty-Third Annual Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Jan. 15, 1953.

INTRODUCTION

IN RECENT years considerable attention has been focused on the heat-treating properties of low-alloy weld deposits. The temper-brittleness problem which has bothered the low-alloy steel users for many years has thus been inevitably encountered by those who are using low-alloy weld deposits.

Temper-brittleness may be defined as the loss in notch-toughness which occurs when certain steels are held within, or are slowly cooled through, a certain range of temperatures below the transformation range.

D. C. Smith and W. G. Rinehart[1] were the first to discuss at length the impact properties of low-alloy weld metal. Loss in impact strength was observed in the 100,000 and 120,000 psi Ni-Mo-V grades when stress relieved between 1050 and 1250° F, although the authors stated that the reduction in the 100,000 psi grade was not so severe as to impair the qualities of the weld metal.

This phenomenon was first brought to the authors' attention when it was reported that a 120,000 psi Ni-Mo-V deposit in 4-in. thick Carilloy-T steel plate showed serious embrittlement after stress relieving at 1150° F, whereas a deposit in 1-in. plate did not show embrittlement. It was suspected that temper brittleness had occurred in the thick plate as a result of slow cooling through the brittle range from the tempering temperature, whereas the cooling rate in the 1-in. plate was rapid enough through this range to avoid being embrittled. In an article[2] by L. C. Bibber and associates, in which the welding of Carilloy T-1 Steel is discussed,

Table 1—Tension Results, 120,000 Psi Ni-Mo-V

(All-weld-metal, 0.505-in. bars, tested at room temperature)

	Yield strength, psi	*Tensile strength, psi*	*Elongation 2 in., %*	*Reduction of area, %*
Unrestrained				
As-welded	121,500	130,000	20	57.5
Tempered*	116,200	123,500	19	57.5
Restrained				
As-welded	125,000	130,000	18	54
Tempered*	122,500	130,500	11	25.5

Charpy Keyhole Impact Results, Ft-Lb (Notch centered in weld groove, normal to weld layers)

	−60° F	*−40° F*	*−20° F*	*0° F*	*70° F*	*150° F*	*200° F*	*250° F*
Unrestrained								
As-welded	27.5	31	31.5	..	30	..	..	..
Tempered*	4.5	26	25.5	..	36	..	..	..
Restrained								
As-welded	..	19	27	22	32	..	..	..
Tempered*	..	..	..	..	8.5	11	11	12.5

* 1150° F (4 hr), air cooled.

the temper-brittle aspects of 120,000 psi Ni-Mo-V weld metal are brought out. Charpy keyhole impact test data show that this weld metal in the stress-relief annealed condition (1100° F) has a ductility transition temperature of +130° F.

In consequence of this report, a few tests were conducted to confirm the temper-brittleness phenomenon. Two sets of duplicate all-weld-metal tensile specimens and Charpy keyhole impact specimens were prepared, one welded under restraint and the other unrestrained, using 120,000 psi Ni-Mo-V weld metal. The welds were deposited as butt welds in $^3/_4$-in. mild steel using a 45° vee-groove, $^1/_2$-in. root opening, $^1/_4$-in. backup. To secure restraint, two of the assemblies were fillet welded all around to a 2-in. thick restraining strong-back plate before depositing the test welds. The restrained and unrestrained welds were each tested in both the as-welded and the tempered* conditions. For the tempering, the plates were held at a temperature of 1150° F for 4 hr and air cooled. The strong-back plates remained intact during the heat treatment. The tensile and Charpy impact results are tabulated in Table 1.

Because of the air cooling, the plates welded to the strong-back cooled more slowly than the unrestrained plates. From the results, it appears that the restraint lowered somewhat the elongation and reduction of area in the tensile test and raised considerably the transition temperature as measured by the Charpy impact tests.

To verify this observation and to extend the study to the 100,000 psi grade, Charpy impact plates were prepared in the restrained and unrestrained conditions for both the 100,000 and 120,000 psi Ni-Mo-V grades. Duplicate Charpy impact specimens were tested at −20 and 70° F. Average energy values of the duplicate results are tabulated in Table 2.

The severity of the embrittlement at −20° F in the case of the 100,000 psi grade has masked any effect of the restraint on this grade. With either testing temperature, however, there is a drop in the Charpy value in the tempered condition for both restrained and unrestrained specimens.

Table 2—Charpy Keyhole Impact Strength, Ft-Lb

Notch centered in weld groove, normal to weld layers.

	120,000 psi —Ni-Mo-V—		*100,000 psi —Ni-Mo-V—*	
	−20° F	*70° F*	*−20° F*	*70° F*
Unrestrained				
As-welded	27.5	34	28	34
Tempered*	23.5	28	4	29
Restrained				
As-welded	30.5	31.5	28	34
Tempered*	6.5	25	4	27

* 1150° F (4 hr), air cooled.

The phenomenon has since been confirmed by other laboratories. Because of this low notch toughness in the 120,000 psi grade in many applications, especially in the armor welding field, instructions have been issued not to use this grade if stress-relieving heat treatments are to be employed.

A systematic investigation of temper brittleness in various low-alloy weld metals was obviously in order, since considerably more information on this problem was needed than was available at that time. The most promising approach seemed to be to study the effect of time and temperature on the development of brittleness in various low-alloy weld metals, using the method of previous investigators[2, 4] whereby specimens are tempered for various times at various temperatures. In this method, results are plotted on a time-temperature diagram and a clear picture of the phenomenon can be seen.

At first, it was planned to test Charpy impact specimens. Unfortunately the preparation of sufficient specimens to establish reliable data is very time consuming. A mass of data could be obtained in a short time by using a notched-bend specimen which would be simple to prepare. Thus the temper-brittle data in this paper are largely based on a notched-bend test.

* Tempering in ferrous metallurgy, in the strict sense, refers to the heating of the hardened structure below the critical temperature; however, in this report, for the sake of simplicity, it will refer also to the heating of the weld deposit in any condition below the critical temperature.

DEVELOPMENT OF TESTING PROCEDURE

In most studies of notch brittleness the transition curve is obtained using a particular specimen. Osborn[5] and associates define transition temperature as the temperature at which the curve has a value equal to the arithmetic mean of its maximum and minimum values. Thus, if percent cleavage is used as a criterion, the transition temperature is defined as the temperature at which the curve has a value of 50% shear fracture. Osborn also used several other criteria, such as bend angle, energy absorbed by specimen after passing maximum load, percentage contraction in width of specimen beneath the notch, and bend angle at maximum load, in plotting transition curves.

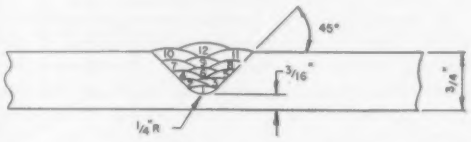
Several investigators[6-9] give much significance to the ductility transition temperature as well as the fracture transition temperature to which the above definition refers. It is observed that as the temperature is decreased considerably below the fracture transition temperature, many specimens continue to absorb a considerable amount of energy even though the fracture face is predominantly granular. Thus a ductility transition temperature is observed in many cases in which the energy absorbed suddenly drops to an extremely low value. In experiments conducted by Osborn and associates[5] on notch-bend tests, it was observed that ductility transition corresponded to change in fracture appearance of a small area beneath the notch.

A notched-bend specimen, when tested at a constant temperature, should thus be capable of giving a clear-cut ductile-or-brittle result (pass-or-fail) if attention is paid to the appearance of the fractured metal.

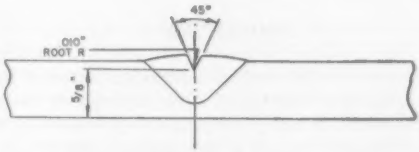
In the development of this test specimen, single-V butt welds were prepared in $^{3}/_{4}$-in. thick mild steel plate with 120,000 psi Ni-Mo-V weld metal. The weld reinforcement was machined flush with the plate metal and the bend-test bars were cut to 2-in. widths and tested in the as-welded and tempered (1150° F for 4 hr) conditions. It was found that the standard V-notch (2-mm depth) used in Charpy and Izod impact tests forced the metal to fracture in the weld. A notch depth of only 1 mm allowed the break to occur in some cases in the base metal.

As a further simplification of the test it was decided to deposit the weld metal in a longitudinal groove machined in $^{3}/_{4}$-in. mild steel plate. A groove having a 60 deg included angle and $^{1}/_{4}$-in. root radius was used. The bottom of the groove was machined $^{5}/_{16}$ in. from the bottom of the plate. The weld was tested without machining off the reinforcement by using a notch whose root was considerably below the level of the natural notches created at the surface of the plate by the fusion line of the weld. The base of the notch was machined at a constant distance ($^{5}/_{8}$ in.) from the bottom of the plate and by so doing, even though the surface of the weld was irregular, a constant amount of metal was tested beneath the root of the notch. The standard V-notch was again used, the dimensions of which are shown in the lower sketch of Fig. 1.

A grooved plate 9 in. long was filled with 120,000 psi Ni-Mo-V weld metal using $^{3}/_{16}$-in. electrodes, and 8 specimens cut to approximately 1-in. widths were prepared. One stress-relieved (1150° F for 4 hr) and one as-welded specimen were tested at each of four different temperatures: 32, 75, 110 and 140° F.



WELD PASS SEQUENCE AND WELD GROOVE DIMENSIONS



NOTCH DETAILS

Fig. 1 Weld pass sequence, weld groove and notch dimensions

The as-welded specimen tested at 32° F had no shear fracture beneath the notch and a narrow shear lip. The specimens tested at 75° F showed wide shear lips and areas of shear fracture beneath the notch. At 110° F the as-welded specimen was quite ductile showing 60 to 70% shear fracture, whereas the 50% fracture point was not approached on the stress-relieved specimens. At 140° F shear structure was observed beneath the notch in the stress-relieved specimens. Applying the current theory of the ductility transition, the as-welded specimen would have a ductility transition temperature between 32 and 75° F whereas the stress-relieved specimen would have a ductility transition temperature between 110 and 140° F.

The above experiment was repeated using the 100,000 psi Ni-Mo-V grade and testing at the same four temperatures. The transition temperatures were somewhat below those obtained for the 120,000 psi grade. The following table shows the ductility transition temperatures for both grades:

	100,000 psi Ni-Mo-V	*120,000 psi Ni-Mo-V*
As-welded, ° F	Below 32	32–75
Stress-relieved, ° F	75–110	110–140

From the above experiments, it was believed that the presence or absence of a shear fracture mode beneath

the notch could be used for judging the brittleness of a specimen. Furthermore, it was believed that contraction in width of the specimen immediately beneath the notch would be a useful criterion for judging brittleness. In the presence of shear fracture beneath the notch, the contraction always measured at least 4%. In the cases where no shear beneath the notch was observed, the contraction was on the order of 2%.

In preparing the test bars for the temper-brittleness study, slight modifications in the preparation of the welding groove and notch were introduced. The groove was enlarged somewhat by machining the root $^3/_{16}$ in. instead of $^5/_{16}$ in. from the bottom of the plate and by changing the included angle to 90 deg. The pass sequence and notch and groove design are shown in Fig. 1. The groove was machined in $^3/_4$-in. mild steel plate, 9 in. wide and 12 in. long, which allowed 11 bend-test bars approximately $^7/_8$ in. in width to be taken from one plate.

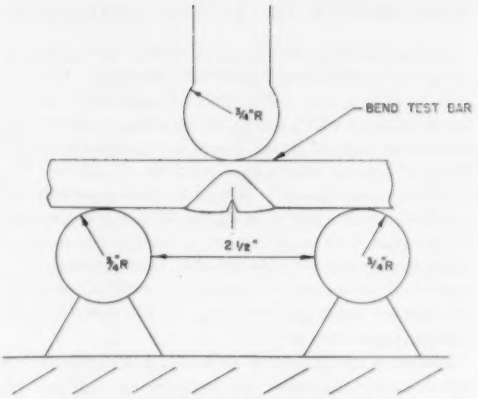


Fig. 2 Working contour of bend test jig

PROCEDURE

Five low-alloy low-hydrogen electrode compositions were obtained in the $^3/_{16}$-in. size. A choice of heats being available in nearly every case, care was taken to select compositions close to the average value of each grade in respect to both chemistry and weld metal tensile properties.

The chemistries and tensile properties of the all-weld-metal compositions are shown in Table 3. Except for the 2Cr–$^1/_2$Mo tensile data, the values are for the as-welded weld metal.

During welding the pass sequence of Fig. 1 was used. The welding details for the different compositions were as follows:

Ni-Mo-V Grades: The assembly is heated to 100° F prior to welding. After the completion of each layer, within 10 min but not before 5 min, the assembly is immersed in boiling water. The assembly is removed from the boiling water within 1 min after the violent boiling of the water has subsided, and the subsequent layer is started when the temperature of the metal has cooled to 100° F.

Mn-Mo Grades: The assembly is heated to 212° F before welding. After the completion of each layer the plate is allowed to rest on the bench until the temperature drops to 200° F. It is then immersed in boiling water for 5 min and the subsequent layer is welded immediately after taking out of the boiling water.

2Cr–$^1/_2$Mo Grade: 400° F preheat and interpass temperature.

The tempering procedure consisted of placing 8 to 10 bend specimens in a small laboratory muffle furnace at the desired temperature, and after the required heating time withdrawing a specimen from the furnace and permitting it to cool to room temperature in still air. It was observed that 10 min was required at 1200° F for the 10 test specimens to come to temperature; this standard allowance was used in all cases. In heat treating the specimens at the other temperatures, it was realized that the true warm-up time might vary slightly, but it was not believed that the amount of error would be significant. The specimen was notched after being heat treated. When austenitizing treatments were required, the specimen was heated at 1650° F for 2 hr and water quenched. The tempering times for the most part were varied between $^1/_2$ and 24 hr; however, in some cases as much as 44 hr were used.

The specimens were fractured in a 60,000-lb Tinius Olsen tensile testing machine at a cross-head speed of approximately 3 in. per minute. Maximum load be-

Table 3—Composition and Properties of Electrodes Studied

No.	*Type*	*C*	*Mn*	*Si*	*Cr*	*Ni*	*Mo*	*V*
7336	120,000 psi Ni-Mo-V	0.054	0.70	0.16	..	1.52	0.88	0.20
7391	100,000 psi Ni-Mo-V	0.058	0.49	0.27	..	1.73	0.53	0.07
7387	2Cr–$^1/_2$Mo	0.170	0.31	0.57	2.47	..	0.57	..
7389	MnMo—low C	0.070	1.65	0.36	..	..	0.36	..
7390	MnMo—medium C	0.170	1.76	0.27	..	..	0.33	..

No.	*Type*	*Yield strength, psi*	*Tensile strength, psi*	*Elongation, 2 in., %*	*Reduction of area, %*
7336	120,000 psi Ni-Mo-V	109,000	118,000	18.5	54
7392	100,000 psi Ni-Mo-V	99,000	110,000	21	53.5
7387	2Cr–$^1/_2$Mo	116,000*	129,000*	19*	62.5*
7389	MnMo—low C	81,000	97,500	18	26.5
7390	MnMo—medium C	92,500	114,500	10	29

* Stress relieved at 1150° F for 2 hr.

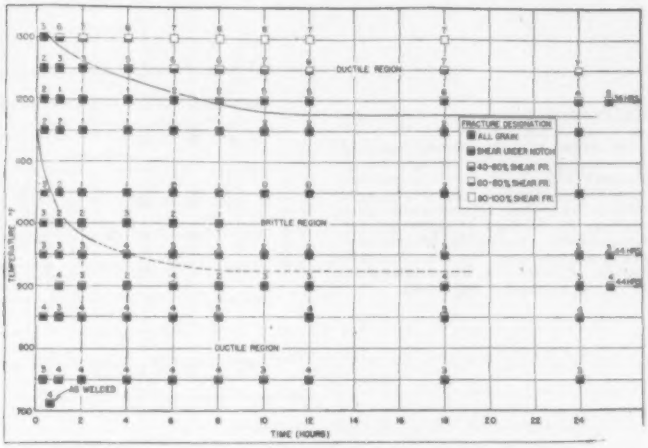


Fig. 3 Notch-bend temper-embrittlement study of 120,000 psi Ni-Mo-V weld metal, showing development of embrittlement during tempering of as-welded specimens. Specimens were broken at room temperature after holding at elevated temperatures for times shown. Numerals indicate percent contraction 1/32 in. beneath notch. Location of lower boundary of brittle region is somewhat uncertain

fore breaking, percent contraction beneath the notch and fracture appearance were recorded for each of the specimens. The working contour of the bend jig is shown in Fig. 2.

OBSERVATIONS

120,000 Psi Ni-Mo-V Grade, As-Welded and Tempered

Temper-brittleness data resulting from the numerous isothermal tempering treatments of the 120,000 psi Ni-Mo-V grade in the as-welded condition are shown in Fig. 3. The numbers above the fracture appearance indications are percent contraction values at the point of maximum contraction immediately beneath the notch. They were rounded out to the nearest whole number. Other data such as bend angle and maximum load were found to be of no value since there was no correlation between these data and the brittle condition. Where more than one test for a given condition was run, average values are shown. Reproducibility of results was good in the 1000 to 1300° F temperature range but poor in the 900 to 950° F range.

The indicated boundary between brittle and ductile states was drawn according to the fracture appearance of the specimens. A specimen was classified in the ductile region if it showed a wide shear lip and a small area of shear fracture immediately below the notch. This appearance is similar to the fracture appearance of the room temperature as-welded specimens and that of the specimens tested at 750–850° F.

According to the currently accepted explanation, the as-welded and 750–850° F specimens tested at 75° F were above the ductility transition temperature, in the lower portion of the fracture transition range. It may be seen from Fig. 3, comparing specimens with shear fracture beneath the notch and those with all-grain fracture, that the former have higher percent contraction with very few exceptions.

Figure 3 shows that serious embrittlement develops very rapidly in the 120,000 psi Ni-Mo-V grade from 1000 to 1200° F, whereas the embrittlement at 1250 and 1300° F is less serious. It is very interesting to note that tempering longer than 10 hr at 1200° F restores the ductility of a welded-and-tempered specimen and that considerably shorter times at 1250 and 1300° F also restore the ductility. At these latter temperatures, moreover, it is significant that increasing time continues to improve the ductility, which after 24 hr at 1250° F and 6 hr at 1300° F is manifested by a predominantly shear fracture. The restoration of ductility with increasing times at 1250° F is clearly shown in Fig. 4. It can be seen that decreasing amounts of

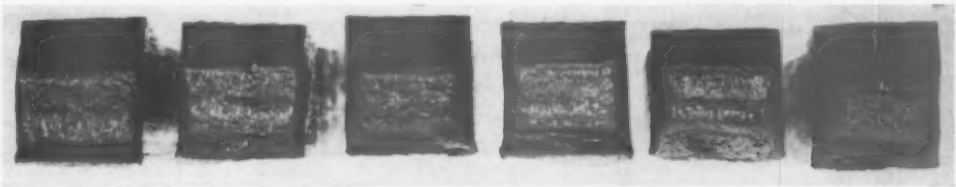


Fig. 4 Fracture faces of bend specimens showing influence of time on the restoration of ductility at 1250° F. Increasing amounts of shear structure can be seen when viewing the specimens from left to right. Tempering times from left to right are: 1/2, 1, 4, 8, 18 and 24 hr.

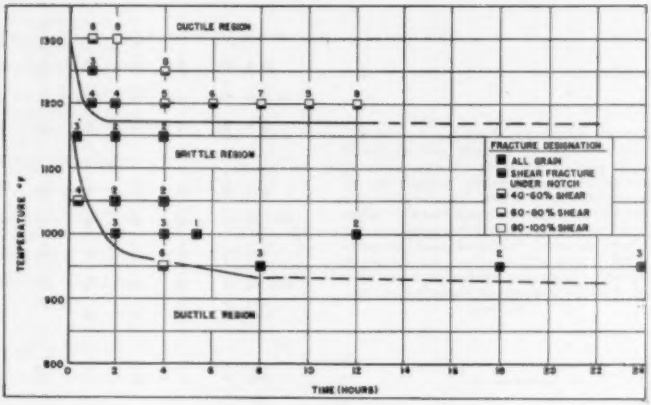


Fig. 5 Notch-bend temper-embrittlement study of 120,000 psi Ni-Mo-V weld metal, showing development of embrittlement during tempering of austenitized (1650° F—2 hr—water quench) specimens. Specimens were broken at room temperature after holding at elevated temperature for time shown. Numerals indicate percent contraction 1/32 in. beneath notch. Location of lower boundary is somewhat uncertain

grain or cleavage structure are present on the fracture faces of the bend test bars as they are viewed from left to right (increasing time at temperature).

The embrittling behavior is not well defined in the lower temperature range. There is some evidence of embrittlement at 900° F, but increasing times did not reveal any serious embrittlement, at least up to 44 hr. The fracture faces of the 900° F specimens appeared to be only slightly more brittle than the as-welded specimens. At 950° F, evidence of embrittlement can be clearly observed; however, reproducibility of results was poor at this temperature so that the exact location of the boundary line could not be determined accurately.

The poor reproducibility of results in the 900–950° F temperature range may be caused by the fact that the testing temperature (75° F) for weld metal tempered in this range is in the lower portion of the ductility transition zone. Thus, because of normal scatter, the specimen may show a completely brittle fracture or one having some shear structure beneath the notch, with corresponding differences in the percent contraction values.

120,000 Psi Ni-Mo-V, Heat Treated and Tempered

Since many weldments require austenitizing heat treatments and since it was believed desirable to explore the effect of various prior heat treatments on the development of temper brittleness, this grade was subjected to various heat treatments before the isothermal tempering treatments. Three heat treatments were studied: (*A*) 1650° F (2 hr), water quench, plus temper; (*B*) 1650° F (2 hr), water quench, plus 1300° F (2 hr), plus temper; (*C*) 1300° F (2 hr), plus temper.

An as-quenched specimen (water quenched from 1650° F) was fractured before investigating the effect of tempering on this prior heat treatment. The as-quenched specimen showed practically an all-shear fracture face, but surprisingly little contraction, about 2.5%. Presumably this was because of the fairly high as-quenched hardness.

(*A*) *1650° F (2 Hr), Water Quenched and Tempered.* As shown in Fig. 5, the brittle condition is also present in the tempered specimens which were subjected to an austenitizing treatment, but the brittleness is not serious and is in fact almost nonexistent in the 1200–1300°

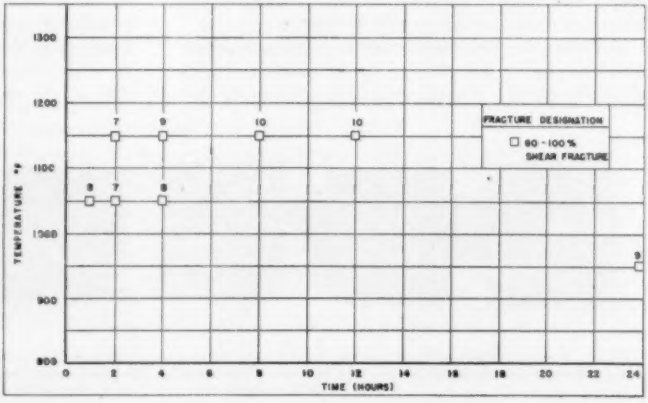


Fig. 6 Notch-bend temper-embrittlement study of austenitized and tempered 120,000 psi Ni-Mo-V weld metal, showing freedom from embrittlement during heat treatment at 1050 and 1150° F. Specimens held at 1650° F (2 hr) tempered 1300° F (2 hr) prior to tempering for times shown

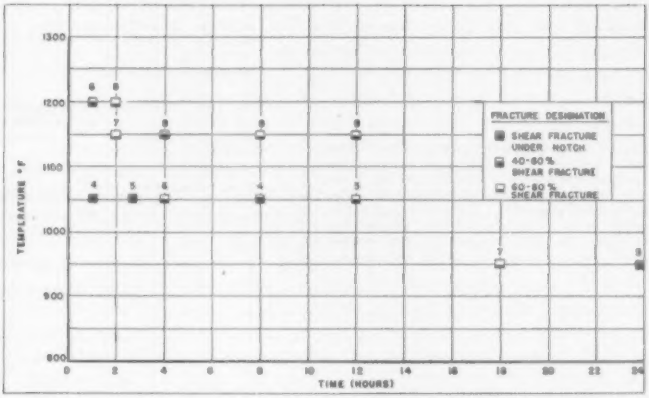


Fig. 7. Notch-bend temper-embrittlement study of 120,000 psi Ni-Mo-V weld metal showing beneficial effect of subcritical tempering treatment (1300° F, 2 hr) prior to tempering for times shown

F temperature range. The embrittlement in the 950–1150° F range is comparable to that of the as-welded specimens; however, at 950° F, good ductility is retained up to at least 4 hr at heat. Nevertheless, similar to the as-welded specimens, the embrittlement is not severe at 950° F, as evidenced by the percent contraction values.

(*B*) *1650° F (2 Hr), Water Quenched, Plus 1300° F (2 Hr), Air Cooled and Tempered.* Figure 6 reveals the complete absence of temper brittleness in 120,000 psi Ni-Mo-V weld metal subjected to an austenitizing treatment and subcritical temper (1300° F for 2 hr) prior to the temper-brittle heat treatments. All specimens showed at least 7% contraction beneath the notch and 90 to 100% shear fracture.

(*c*) *1300° F (2 Hr), Air Cooled and Tempered.* Because of the beneficial results of the 1650 plus 1300° F treatments, it was believed desirable to investigate the effects of only a 1300° F prior heat treatment, in order to see if this would produce similar results; tempering for 2 hr at this temperature had resulted in almost complete ductility (40 to 60% shear).

Figure 7 clearly shows that tempering in the brittle range after the 1300° F treatment also does not impair the ductility of the weld metal. The ductility is not so high as after the 1650 plus 1300° F treatment, but this would be expected on the basis of the relative ductility after these two prior heat treatments. The 1600 plus 1300° F treatment produces an almost completely shear fracture, whereas the 1300° F (2 hr) treatment results in 40 to 60% shear fracture.

100,000 Ni-Mo-V, as-Welded and Tempered

Temper-brittleness data for isothermally tempered bars prepared with the 100,000 psi Ni-Mo-V electrode are summarized in Fig. 8. The data confirm the results in the section on "Development of Testing Procedure" that this electrode deposits somewhat more ductile weld metal than the 120,000 psi grade. The fracture faces showed about 40 to 60% shear structure and an average value of 6% contraction beneath the notch. Comparison of this figure with Fig. 3 makes it quite apparent that the 100,000 psi grade is considerably less subject to temper-brittleness than the 120,000 psi grade.

Figure 8 shows that the range (approximately 1000 to 1150° F) in which temper brittleness occurs is more narrow and the loss in ductility in this range, as indicated by percent contraction values, is much less than

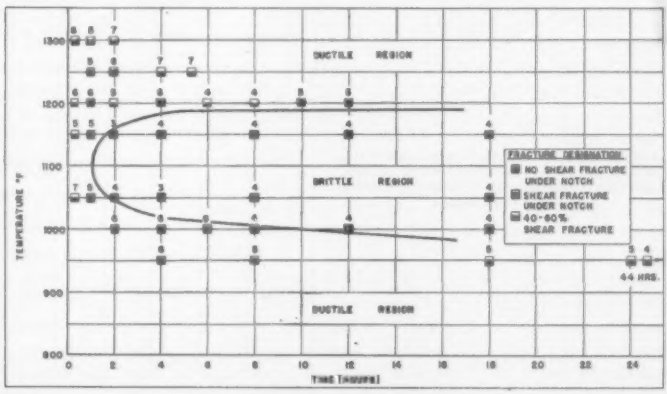


Fig. 8 Notch-bend temper-embrittlement study of 100,000 psi Ni-Mo-V weld metal showing development of embrittlement during tempering of as-welded specimens. Specimens were broken at room temperature after holding at elevated temperatures for times shown

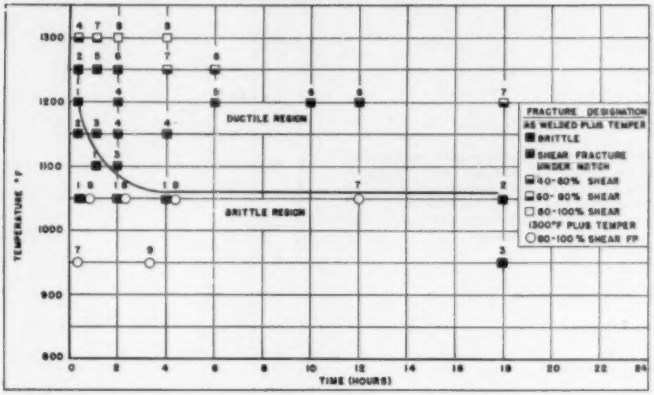


Fig. 9 Brittle range in 2 Cr-1/2Mo weld metal. Metal is not subject to temper brittleness, but brittle by reason of insufficient tempering of the as-welded deposit. Tempering of softened metal (indicated on graph by circles) in the temper-brittle range does not decrease the notch toughness

in the brittle range of the 120,000 psi grade. The lowest contraction value is 3%, which is only slightly lower than the average value for the as-welded 120,000 psi grades. Unlike the 120,000 psi grade, there is no apparent temper brittleness resulting from short times at higher temperatures (1200 to 1300° F).

2%Cr - 1/2%Mo Grade

The data for a 2%Cr–1/2%Mo electrode are presented in Fig. 9. Fracturing of the as-welded 2% Cr–1/2%Mo deposit indicated its extreme brittleness in the absence of heat treatment. The fracture face was 100% granular and the percent contraction beneath the notch was about 1%.

Examination of Fig. 9 shows that this grade is not subject to temper brittleness. Tempering for sufficient times in the temper-brittle range reduces or eliminates the as-welded brittleness, which is an inherent property of this composition. The graph shows the importance of time at temperature. As in the previous plotting of temper-brittle data, the line between brittle and ductile specimens was drawn between the completely granular indications and those where shear fracture beneath the notch was observed. As can be seen from the figure, the specimens showing shear fracture below the notch generally had higher percent contraction values.

In order to confirm the absence of temper-brittleness, specimens were held at 1300° F for 2 hr to achieve maximum ductility, and then were tempered at 950 and at 1050° F. Figure 8 shows the excellent ductility of these specimens.

Mn-Mo Grade—Low and Medium Carbon

The two manganese-molybdenum electrode grades showed complete freedom from temper brittleness. Moreover, both of the grades exhibited good ductility in the as-welded condition, with completely shear fracture showing about 7% contraction for the low-carbon grade and about 4% contraction for the higher-carbon grade.

Figure 10 shows the percent contraction values obtained for these two grades after tempering at different temperature levels for varying times. It can be seen from the symbols that all fractures were 100% shear.

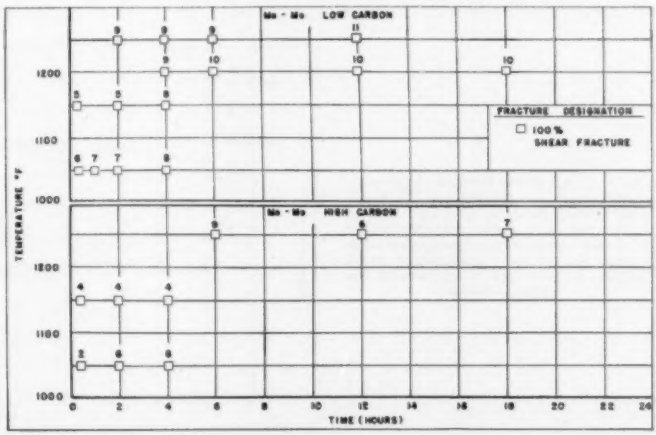


Fig. 10 Notch-bend temper-embrittlement study of low- and high-carbon Mn-Mo weld metal showing freedom from brittleness

As would be expected, the higher carbon grade shows lower percent contraction values.

Effect of Tempering on Tensile Properties of 120,000 Psi Ni-Mo-V Grade

The long tempering time in the 1250–1300° F range necessary to eliminate brittleness raises the question as to the influence of time at temperature on the strength properties of the weld deposit. The effect of time at temperature in the 1050–1150° F brittle range may also be questioned. Consequently, this factor was investigated at four temperature levels: 1050, 1150, 1250 and 1300° F. At 1250 and 1300° F the times used were varied in order to measure the strengths in both the brittle and ductile region (see Fig. 3). Standard all-weld-metal tensile plates were prepared for the 120,000 psi Ni-Mo-V grade and the results are tabulated in Table 4.

Table 4—120,000 Psi Ni-Mo-V Tension Tests

All-weld-metal 0.505-in. bars, tested at room temperature.

Tempering Temperature	*Time, Hours*	*Yield Strength, psi*	*Tensile Strength, psi*	*Elongation 2 in., %*	*Reduction of Area, %*
As-Welded	...	109,000	118,000	18.5	54
1050° F	4	118,000	129,000	19	55
1050° F	12	122,000	132,000	16*	54
1150° F	4	118,000	126,000	16*	50
1150° F	12	110,000	118,000	19	54
1250° F	2	101,000	108,000	20	54
1250° F	6	83,000	102,000	20	55
1250° F	24	87,000	99,000	21	60
1300° F	1/2	112,000	120,000	17	47
1300° F	2	99,000	109,000	21	59

* Specimen broke near gage mark.

The results show that except in the case of the specimens tempered at 1050° F, an increase in time at temperature decreases the strength properties of this weld metal. The 6-hr tempering treatment at 1250° F, which is necessary to eliminate the brittleness at this temperature level, results in quite a considerable drop in strength. The 2-hr treatment at 1300° F which gives satisfactory notch ductility, apparently does not reduce the strength properties quite so much as the 1250° F 6-hr treatment. Tempering for 4 to 12 hr at 1050° F or for 4 hr at 1150° F results in a significant increase in the strength properties over those in the as-welded condition. The loss in notch toughness from this temper-embrittling treatment is not reflected in the elongation or reduction of area values, however.

GENERAL DISCUSSION

The tendency toward temper brittleness of certain low-alloy weld metal grades has been clearly demonstrated by these isothermal tempering treatments. By tempering notched specimens for different times at different temperatures and observing the manner of fracturing after slow bending, the rate at which the embrittlement develops has been observed.

Of the five weld metals tested, only the Ni-Mo-V grades appear to be unsatisfactory from the temper-brittle standpoint. The 100,000 psi Ni-Mo-V grade is not so seriously affected by this phenomenon as the 120,000 psi Ni-Mo-V grade. No indication of temper brittleness was revealed in the other three weld metal grades at the testing temperature used.

The possibility of restoring the ductility of embrittled specimens by long heating times at 1200° F and by somewhat shorter times at 1250 and 1300° F may prove significant in applications requiring stress relieving. The realization that fairly long heating times at 1250° F result in good notch toughness may make it possible to remove the present restrictions against stress relieving. It was observed in this investigation that at least 4 hr are necessary at 1250° F to eliminate the embrittlement effect.

The reduction in strength of the metal after these long tempering times may be objectionable in some applications.

Although the mechanism of embrittlement reactions is not well understood, such effects are generally believed to be the result of a precipitation reaction. Thus overaging may take place at the higher temperatures to restore the ductility of the weld metal.

The effect of prior heat treatments is believed to be very significant. The simple introduction of an austenitizing treatment (1650° F, water quench) subsequent to welding appears to have been quite beneficial. Although the notched ductility of the quenched and tempered Ni-Mo-V weld metal is low in the 1050–1150° F range, there is noticeable improvement in the 1200–1250° F range, and an apparent retardation of the rate of embrittlement at 950° F. This observation appears to be confirmed by Smith and Rinehart,[1] who found that a relatively high impact value was obtained for the 120,000 psi Ni-Mo-V grade after quenching from 1650° F and tempering for 4 hr at 1250° F; they obtained a low value after tempering only 1 hr at this temperature. The data appear in their Table 4 without further elaboration.

The beneficial effects of the other two prior heat treatments—1650° F (water quench) plus 1300° F (2 hr), and 1300° F (2 hr)—are quite remarkable. Complete ductility in the 1050–1150° F range is observed in the tempered specimens which had been subjected to the 1650 plus 1300° F treatment, and very significant improvement is observed in the tempered specimens after the 1300° F treatment. Undoubtedly a longer time at 1300° F would have further improved the ductility in these tempered specimens, since it may be noted from Fig. 3 that complete ductility is not observed in the as-welded specimen that was tempered for 2 hr at 1300° F.

The beneficial effect of prior heat treatments indicates a solution (at least in the case of Ni-Mo-V weld metal) to the dilemma which temper brittleness creates in the case of heavy weldments. Heavy sections usually must be stress relieved. To avoid embrittlement the weldment is therefore quenched from above

the temper-brittle range to room temperature, in an attempt to avoid slow cooling through the brittle range. But in heavy sections the cooling rate even by quenching may be too slow to avoid embrittlement in this range. However, judging from the results of the prior heat treatments, especially the 1650 plus 1300° F and the 1300° F treatments, slow cooling through the serious range (1050 to 1150° F) can be tolerated if the weldment was held at a higher temperature for a long enough time. Smith and Rinehart[1] show high impact values in the 120,000 psi Ni-Mo-V grade after furnace cooling from 1350° F, which apparently confirms the above conclusion. Apparently a specimen may be slowly cooled through the temper-brittle range after it has been subjected to a 1300° F tempering treatment.

CONCLUSIONS

From the results of this investigation the following conclusions appear to be justified:

1. The 120,000 psi Ni-Mo-V grade should not be used if tempering of the as-welded or quench-hardened deposit is to be done in the 1050–1150° F range.

2. If tempering at higher temperatures (1250 to 1300° F) is permitted, the 120,000 psi Ni-Mo-V grade can be used with the following precaution: the metal should be held at 1250° F for at least 4 hr, or at 1300° F for at least 2 hr. A reduction in strength of 10,000 to 20,000 psi will result from tempering at these temperatures.

3. Slow cooling of the 120,000 psi grade can be permitted from the 1300° F tempering temperature (and probably from 1250° F if held for a sufficient time). Better results may be obtained, however, if the metal is subjected to a prior austenitizing heat treatment.

4. The 100,000 psi Ni-Mo-V grade may be tempered in the 1050–1150° F range, if not over 1 hr at temperature is required. Tempering at higher temperatures results in very little embrittlement.

5. Temper brittleness is not revealed in the Mn-Mo and 2Cr–$^1/_2$Mo electrode grades, in the range of temperatures between 900 and 1300° F.

ACKNOWLEDGMENT

Thanks are due R. David Thomas, Jr., who authorized this research project and suggested many of the investigations which were made. His critical review of several drafts of the manuscript greatly improved the final paper.

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(*Continued on page 542-s*)

Brittle Strength and Transition Temperature of Structural Steel

A study and interpretation of test data regarding brittle strength and transition temperature from wide plate tests

by W. C. Hoeltje and N. M. Newmark

Summary

This paper presents the results of a study of the available test data regarding brittle strength and transition temperature from the wide plate tests sponsored by the Ship Structure Committee and affiliated programs. The study includes (1) brittle strength and (2) transition temperature range between brittle and ductile fracture. Both of these appear to be related primarily to a variable designated herein as ρ, the ratio of the radius at the root of the notch to the net cross-sectional area of the specimen.

The brittle strength shows a general tendency to increase with a corresponding increase in ρ for a considerable range, which covers all values of the variable for which data could be obtained. On the other hand, the transition temperature tends to remain practically constant for all values of ρ less than some critical value, whereas above this critical value transition temperature appears to drop as ρ increases.

Some of the discrepancies that have been observed in the strength and transition temperature of small specimens relative to large specimens may arise from the influence of the parameter ρ. In general, for geometrically similar specimens, the value of ρ is numerically larger for the smaller specimens. This perhaps explains why small specimens tests are in general less severe than are tests of large specimens or tests of prototype structures.

INTRODUCTION

History

DURING World War II many ships were fabricated by welding. Because a number of failures occurred by brittle fracture, an extensive experimental program was initiated to investigate the causes of failure. Several heats of structural grade steel, designated project steels, were obtained and distributed for testing to the various research laboratories which were to participate in the investigation.

The main tests were conducted on $^3/_4$-in. thick internally notched tension specimens 12, 24, 48 and 72 in. wide.[1–5] Additional tests were made on smaller specimens in affiliated programs.[6–8, 12–14] The wide plates were tested because ship plates are wider than plates that can be economically and conveniently tested in the laboratory. It was expected that the information obtained from the wide plate tests would indicate the behavior of even wider plates. In addition it was hoped that eventually a small-scale specimen, economical to test, could be developed which would give results correlating with those from the tests on the wide plates. If a small-specimen test proved successful, the behavior of wide ship plates could be predicted from small laboratory specimens.

The wide plate tests yielded a wealth of valuable data and marked a major contribution to our understanding of fracture in plates. However, certain discrepancies appeared in the test results, the most serious of which involved two important characteristics, strength and transition temperature. Strength seemed to vary with changes in notch radius and section size. The transition temperature of the wide plates did not vary significantly as the width changed from 12 to 72 in. but when attempts were made to develop small specimens to duplicate the behavior of the large specimens, discrepancies were observed in both the strength and the transition temperature. These discrepancies have been the basis of much controversy in the past several years.

Object and Scope

A great deal of data on the fracture of plates of structural steel has been accumulated. Certain discrepancies are apparent in the data. The purpose of this paper, therefore, is to investigate the data from the wide plate and affiliated tests and attempt to explain the effects of notch severity and section size on (1) brittle strength and (2) transition temperature.

Brittle strength rather than ductile strength is selected for study herein. Generally only the nominal strength (maximum load divided by original net area) is recorded in the available data. When a specimen fails in a ductile manner, the nominal strength has little

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physical meaning in terms of the fracture strength because of the large reduction in area at fracture. In the brittle condition, however, the nominal strength is very close to the fracture strength. For this reason results are confined to specimens tested below the transition temperature of the material as defined by the particular specimen tested. This does not mean to imply that the brittle state is of more importance than the ductile state. It merely means that at the present stage of development the correlation of strength reported herein is valid only in the brittle state.

Three types of tensile specimens, shown in Fig. 1, were studied. Type A is an internally notched plate in which the stress-raiser terminates in either a drilled hole or a jeweler's saw-cut; most of the wide plate tests were conducted on this type of specimen. Type B is a circumferentially notched cylinder. Type C is an externally notched plate.

Previous Correlations

On the basis of results from tear-test specimens, Bagsar[10] suggested the following equation for relating nominal breaking stresses to notch severity:

$$S = KR^{0.113} \qquad (1)$$

where S is the breaking load divided by the original cross-sectional area, K is a constant and R is the radius of the notch at the root.

Since the wide plate tests indicated that the strength varies not only with the notch radius but also with the section size, the writers included a second factor in eq 1 and studied strength in terms of a new parameter, $\rho = R/A$, where R is the radius at the root of the notch and A is the net cross-sectional area of the specimen. The influence of this new variable on transition temperature is also studied.

It may be noted that the parameter ρ is not dimensionless. It would be logical to use as a variable a quantity which involves ρ multiplied by some fundamental length associated with the material, possibly grain size. Such a variable would be dimensionless. However, since no data are available on the grain size in the tests studied, this refinement was not made in the paper.

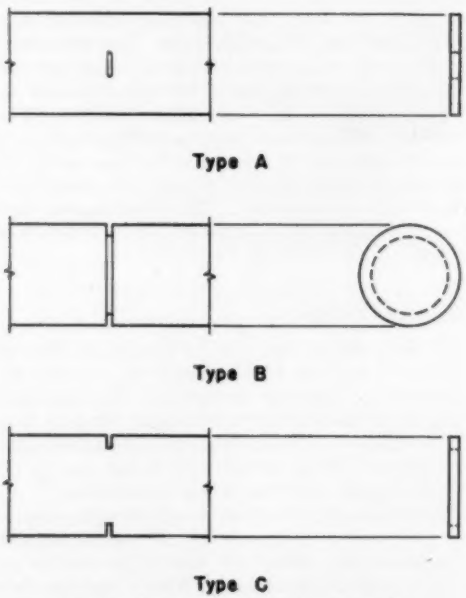


Fig. 1 Types of specimens investigated

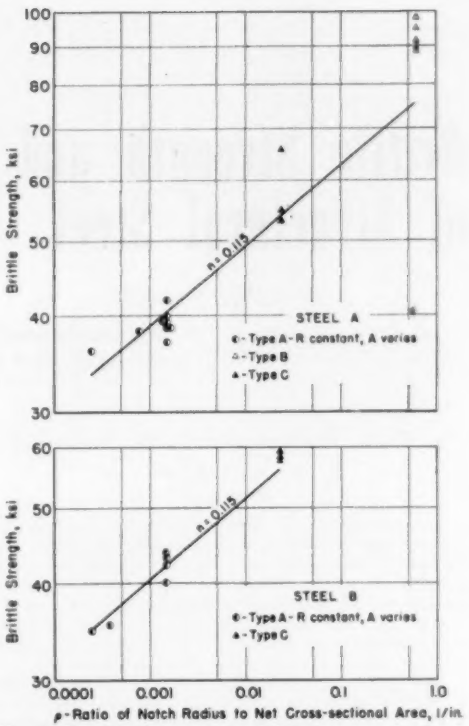


Fig. 2 Relation between ρ and brittle strength, Steels A and B

BRITTLE STRENGTH

Effect of ρ

A preliminary investigation of the data showed that the brittle strengths of those series of specimens in which only the notch radius was varied conformed to eq 1. The value of the exponent in this relation was found to be approximately $n = 0.115$, which confirms the value of $n = 0.113$ reported by Bagsar.

Preliminary work also revealed that the brittle strength of those series of specimens in which only the

section size was varied, was given quite well by a similar equation involving the reciprocal of the net cross-sectional area instead of the notch radius, the value of the exponent also being $n = 0.115$. This, of course, suggested the possibility of combining the two factors to form a new variable, R/A, which is designated by ρ in this paper.

In Figs. 2–8 brittle strength is plotted against ρ, with a logarithmic scale. The plots for the various steels can be approximated by parallel curves having a slope of $n = 0.115$. These curves can be described by the equation:

$$S = K\rho^{0.115} \qquad (2)$$

where S is the brittle strength (maximum load divided by original net area), K is a constant dependent primarily on the material and ρ is the ratio of the notch radius to the net cross-sectional area.

For those specimens containing saw-cuts for stress-raisers, the strengths fit the plots for the round stress-raisers if an equivalent radius is used equal to the *width* of the saw-cut.

Preliminary investigation revealed that in the ranges studied, no consistent effect of temperature on brittle strength was apparent. For this reason the data represented by the plots include the strengths for all the temperatures below the transition temperature of the specimen. This does not imply that the temperature effect should be ignored as it probably contributes to the scatter.

As indicated in Figs. 2–8 most of the plotted points

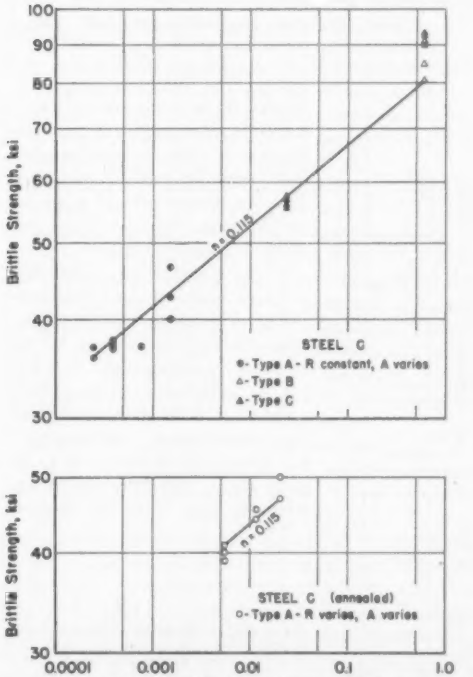


Fig. 3 Relation between ρ and brittle strength, Steels C and C (annealed)

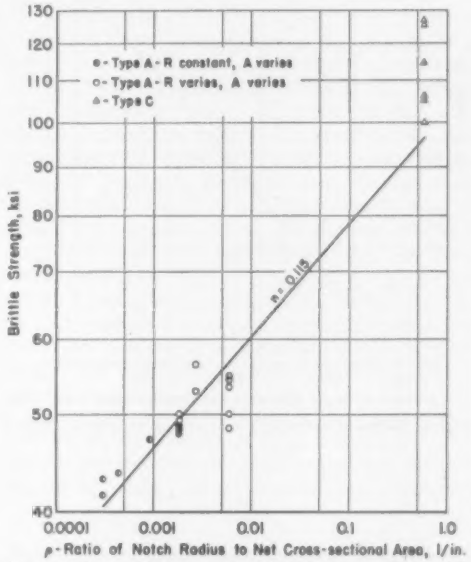


Fig. 4 Relation between ρ and brittle strength, Steel D

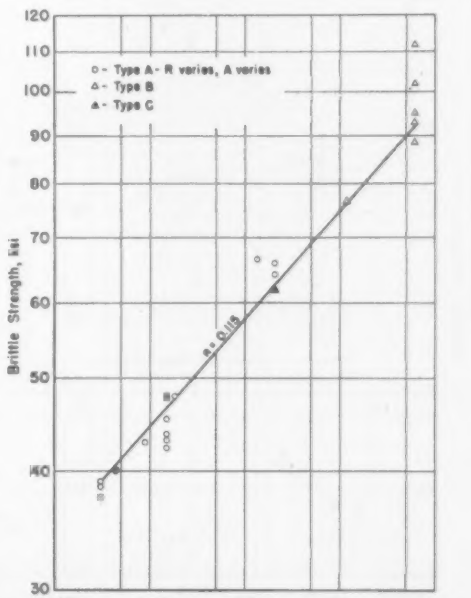


Fig. 5 Relation between ρ and brittle strength, Steel D

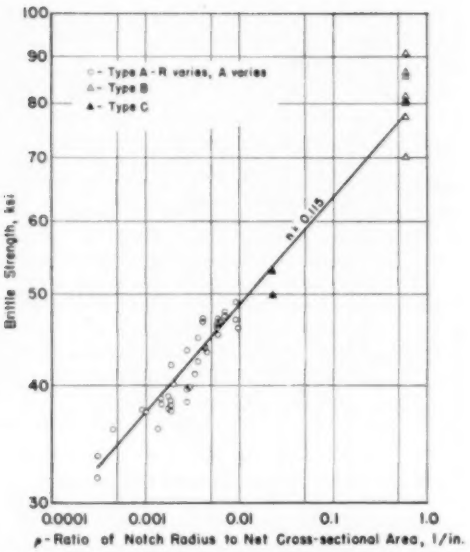


Fig. 6 Relation between ρ and brittle strength, Steel E

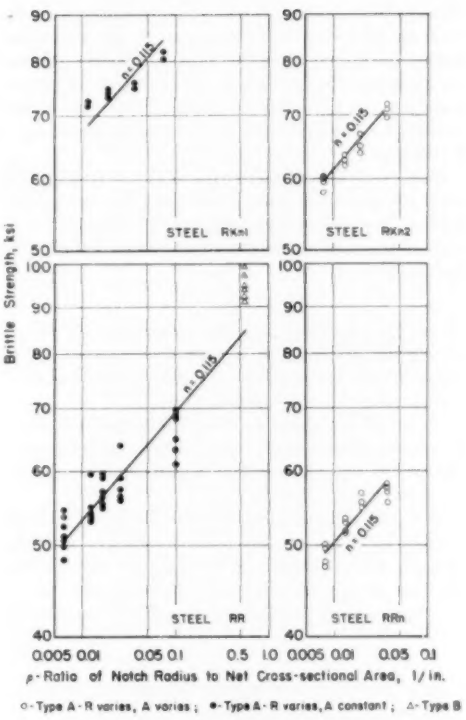
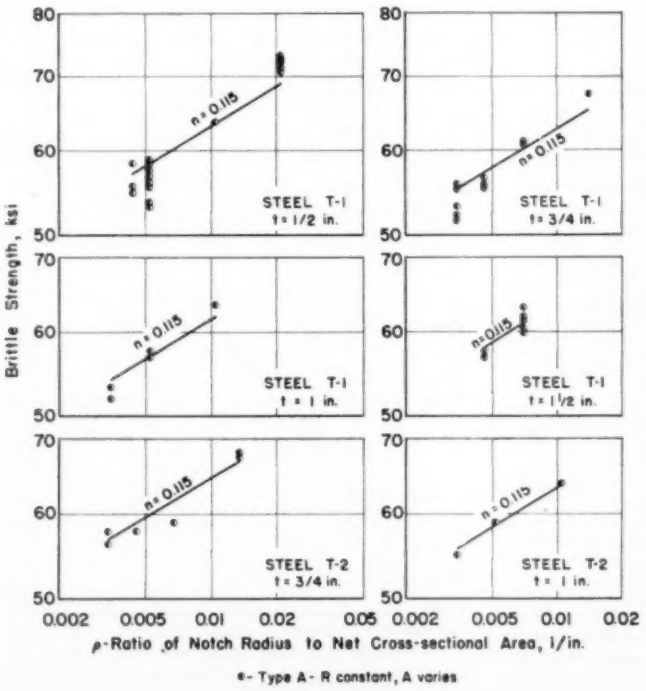

STEEL RKn1
STEEL RKn2
STEEL RR
STEEL RRn
n = 0.115
Brittle Strength, ksi
ρ - Ratio of Notch Radius to Net Cross-sectional Area, 1/in.
○ - Type A - R varies, A varies ; ● - Type A - R varies, A constant ; △ - Type B

Fig. 7 (Right) Relation between ρ and brittle strength, Steels RKn1, RKn2, RR and RRn (see References 6 and 7)


STEEL T-1 t = 1/2 in.
STEEL T-1 t = 3/4 in.
STEEL T-1 t = 1 in.
STEEL T-1 t = 1 1/2 in.
STEEL T-2 t = 3/4 in.
STEEL T-2 t = 1 in.
n = 0.115
Brittle Strength, ksi
ρ - Ratio of Notch Radius to Net Cross-sectional Area, 1/in.
● - Type A - R constant, A varies

are for Type A specimens, and it was from these points that eq 2 was developed. Also included are data for Types B and C specimens. The brittle strengths of these latter two types were correlated with those of Type A as follows:

If two types of specimens, such as types A and B, have the same values of R and A, their respective stress-concentration factors will not be equal. Consequently, eq 2 cannot be expected to yield a direct correlation of the strengths of both types of specimens. Before plotting the strength of Specimen B on the same graph with Specimen A, the strength must first be modified

Fig. 8 (Left) Relation between ρ and brittle strength, Steels T-1 and T-2. (See Reference 14)

by the ratio of the stress-concentration factors to obtain the strength of an equivalent Specimen A, in accordance with the following equation:

$$S_A' = S_B \frac{C_B}{C_A} \qquad (3)$$

where S_A' is the strength of an equivalent Specimen A having values of R and A the same as Specimen B, S_B is the actual strength of Specimen B and C_A and C_B are the theoretical stress-concentration factors for Specimens A and B, respectively.

The brittle strengths of specimen Types B and C were modified in accordance with eq 3 before plotting. The theoretical stress-concentration factors were determined by the method of Neuber.[11]

Significance of Experimental Results

Included in Figs. 2–8 are the results of tests on series of specimens in which (1) only width varied, (2) only notch radius varied and (3) width, thickness and notch radius varied. Included in (3) are geometrically similar specimens. No separate trends in the behavior of strength can be noted in relation to any one of the three methods of varying the geometry. Within the ranges studied, the main dependency of strength is evidently on the variable ρ only, regardless of how the change in geometry is effected. The ranges of geometry included in these plots are as follows: thickness, 0.180 to $1^1/_2$ in.; width, $1^1/_2$ to 72 in.; and notch radius, 0.007 to 0.125 in. Further experimental work is required before the geometrical limits of application of eq 2 can be determined. It may be that beyond some limiting value of the ratio of notch radius to thickness or width, as a possibility, the equation may not hold true.

Inspection of eq 2 shows that the brittle strength can be related to the ratio of the notch radius to the net cross-sectional area, irrespective of the magnitudes of either of these two factors. If the value of ρ for a small specimen is the same as that for a large specimen, the strengths of the two specimens should be equal. Utilizing this relationship it may be possible to select a small specimen to give the same strength as a large specimen. However, the acuity of the notch in the small specimen will have to be greater in inverse proportion to the relative sizes of the specimens.

Equation 2 holds for the complete ranges of ρ included in the tests referred to. At some upper and lower limits of ρ there should probably occur a break in the curves to horizontal lines, i.e., maximum and minimum strengths might be reached at which points further changes in ρ would have no appreciable influence on strength. If a lower limit for minimum strength exists, the value of ρ must necessarily be small. It is at this part of the curve where ρ is small that simulation of service behavior may be more closely approached because the value of ρ for an actual structure is also small (crack in a plate or notch radius is small and area is large). This condition should be kept in mind in selecting the geometry of a small-scale acceptance test specimen.

TRANSITION TEMPERATURE

Effect of ρ

The transition temperatures of the project steels did not vary significantly when determined by either 12-, 24-, 48- or 72-in. wide specimens having jeweler's saw-cuts for stress-raisers. Yet when attempts were made to duplicate the behavior of these large specimens with small specimens, the transition temperatures generally decreased. In some cases the transition temperatures of the small specimens were the same as those of the large specimens and in other cases they were lower. To date these discrepancies have not been satisfactorily explained.

The difficulty in explaining the discrepancies is that no real common denominator among specimens has been used on which to base comparisons of the effect of geometry. A possible common basis is the variable ρ.

In Figs. 9–11 transition temperature is plotted against ρ for the available results on Type A specimens. The logarithmic scale of the abscissa has no significance other than for convenience of plotting and to facilitate com-

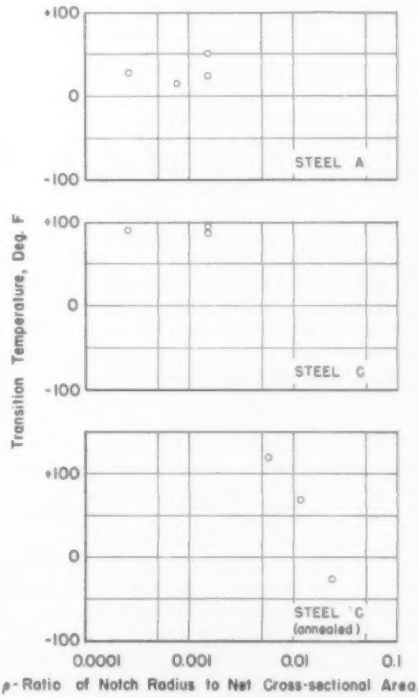


Fig. 9 Effect of ρ on transition temperature of Type A specimens, Steels A, C and C (annealed)

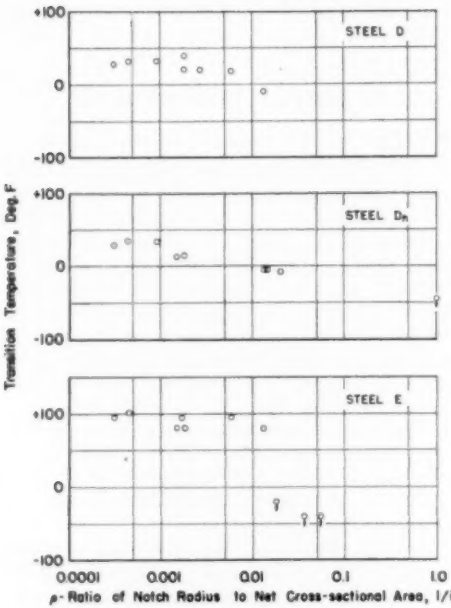


Fig. 10 Effect of ρ on transition temperature of Type A specimens, Steels D, D_n and E

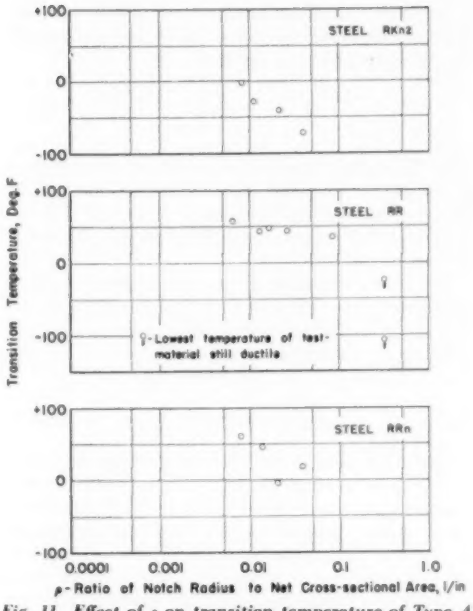


Fig. 11 Effect of ρ on transition temperature of Type A specimens, Steels RKn2, RR and RRn (see References 6 and 7)

parisons. The criterion used for selecting the transition temperatures is energy absorption.

Examination of the plot for Steel E in Fig. 10 shows that for values of ρ less than about 0.013 in.$^{-1}$ there is no significant change in the transition temperature as ρ decreases, i.e., the transition temperature is at an upper limit. Above this critical value there is an effect which involves generally a reduction in the transition temperature. Not enough points are available in this critical range to determine whether the variation is smooth or erratic. Similar behavior occurs at somewhat different horizontal locations for the other steels in those cases where the geometries of the specimens tested gave sufficiently large values of ρ.

Significance of Experimental Results

Indications from Figs. 9–11 are that there is a critical range of ρ in which changing the geometry will affect the transition temperature of the material. Below this range the transition temperature has an upper limit and is not appreciably affected by further decreases in ρ. The horizontal location of the critical range is evidently dependent on the material.

On the above basis it may be possible to explain why the small-scale specimens behaved differently from the large specimens. The larger specimens had values of ρ less than the critical value—their geometries were in a range where further decreases in ρ had no appreciable

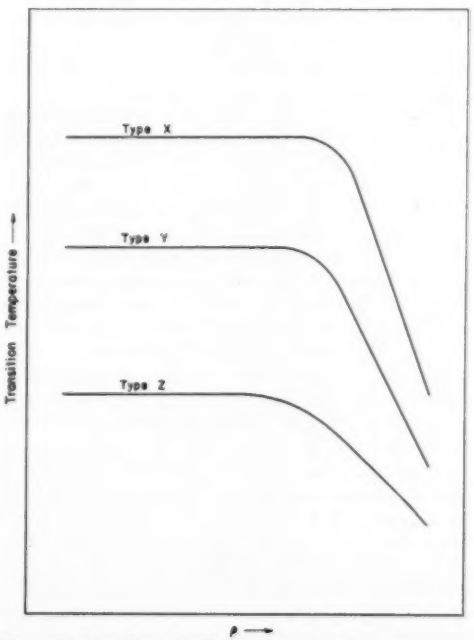


Fig. 12 Hypothetical illustration of upper limits of transition temperature for different types of specimens

effect. Therefore, the transition temperatures were approximately the same when determined by either 12-, 24-, 48- or 72-in. wide specimens containing jeweler's saw-cuts for stress-raisers. On the other hand the geometries of the small specimens gave values of ρ greater than the critical value where geometry *does* affect the transition temperature. In the respective ranges the behaviors are evidently quite different. Consequently, discrepancies arose when the behavior of the small specimens was compared with that of the large specimens.

With the above hypothesis as a basis it may be possible to select the geometry of a small specimen to give the same transition temperature as that of a large specimen. Since the transition temperature can be related to ρ, it appears feasible that for a small specimen to correlate with a large specimen, it should have either the same value of ρ or a value less than the critical value.

A closer simulation of service behavior may be achieved if ρ is less than the critical value. In this range the transition temperature is at its upper limit and therefore approaches the condition, for example, of a crack in a wide plate.

If ρ is less than the critical value, the transition temperature is at its upper limit *for a given type of specimen* only. The upper limit may be quite different for other types of specimens, as shown hypothetically in Fig. 12. This hypothesis may prove of value in clarifying the discrepancies in transition temperature as determined by tests of different types of specimens.

Correlation studies of different types of specimens and testing conditions may be greatly simplified by working with specimens whose values of ρ are less than critical, because the effects of variations due to geometry are thereby substantially eliminated.

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Embrittlement of High-Strength Ferritic Welds

An investigation of the effect of thermal stress relieving of high-strength ferritic welds of the Mn-Mo and Ni-Mo-V types

by Peter P. Puzak and William S. Pellini

Abstract

The effect of stress-relieving heat treatments of low-hydrogen, high-strength, ferritic welds of the Mn-Mo (Mil. E986—Ships Grade 230) and Ni-Mo-V (Grade 260) types were investigated to determine changes in properties which would be expected to affect the performance of weldments. It was established that the Mn-Mo type is essentially unaffected by stress-relief heat treatments. The Ni-Mo-V type exhibits a pronounced loss in notch toughness which is ascribed to the precipitation of vanadium carbides, similar to "age-hardening" reactions. "Overaging" resulting from prolonged holding at stress-relieving temperatures results in complete recovery of notch toughness. However, the strength properties are decreased considerably below the level of the as-deposited weld metal. It is concluded that the subject Ni-Mo-V welds are not suitable for stress-relieving heat treatment.

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INTRODUCTION

THE low-hydrogen, high-strength, ferritic electrodes which were developed during the war years for the welding of high-alloy steels have been adopted for industrial use during the past few years. The high strength of the weld metal deposited by these electrodes provides the required matching of tensile properties of high-tensile constructional steels. The low-hydrogen feature of the electrodes is known to be essential for the prevention of cold cracking in the heat-affected zone of the high-tensile steels and for the elimination of fissures in the weld proper. Experience has shown that as the tensile yield strength of the weld metal is increased to match the yield strength of the base metal the tolerable limit of hydrogen is lowered. It is known also that the high notch toughness of these welds is a primary factor determining proper performance in service. The desired features

of these electrodes are ensured by the new military specifications (Mil. E986—Ships) which require notch-toughness determinations and evaluation of proper performance under restrained weld joint conditions. Inasmuch as these requirements do not form a part of the AWS specification system, the military specifications classify the specialized electrodes by a "grade number" system. The strength levels which correspond to AWS 100 and AWS 120 are represented by grades 230 and 260, respectively.

The most widely used electrodes which meet the requirement of the 230 and 260 grades are the Mn-Mo (230) and the Ni-Mo-V (230 and 260) alloy types. The Mn-Mo type was used extensively during the war years for armor weldments which were to be given a subsequent heat treatment (casting repair and cast-weld assemblies). The Ni-Mo-V type was developed originally for shipyard welding and was designed to be used without preheat or post heat treatments. Experience with stress-relieved weldments of the grade 260 Ni-Mo-V type has indicated that heat treatment has a detrimental effect on the notch toughness of the weld metal. Recently Smith and Rinehart[1] have reported data which indicated a pronounced embrittlement resulting from heating in the common stress-relieving temperature range. The embrittlement was tentatively ascribed to the precipitation of vanadium and molybdenum carbides. The possibility of temper embrittlement has been considered by others. The available data on the embrittlement characteristics of the subject weld metal do not permit a differentiation between the two possible mechanisms; moreover the relative severity of embrittlement at various temperatures in the stress-relieving range has not been established. Such information is of practical significance from three viewpoints:

1. If the process entails temper embrittlement, a possibility exists that stress relieving at high subcritical temperatures followed by rapid cooling would serve to prevent damage.

2. If the process entails a precipitation analogous to an age-hardening system, a possibility exists that overaging effects obtained by prolonged holding would serve to recover notch toughness.

3. Information as to the embrittlement mechanism is required for a logical approach to alloy modification of the present composition.

This investigation was aimed at determining the kinetics of the embrittlement process so that remedial procedures, if any, could be evolved. The major efforts were directed at investigating the Ni-Mo-V (260) type. Additional studies were conducted on the Mn-Mo (230) type in order to determine the response to stress-relieving treatments for this type. Also, full thickness Ni-Mo-V (260) weldments of 1-in. thick, high-tensile steel in as-welded and stress-relieved conditions were tested over a range of temperatures by means of the Explosion Bulge Technique, to evaluate the relative effects of the weld embrittlement on the performance of the weldments.

MATERIALS AND TEST PROCEDURES

Double V-butt weldments of 1-in. thick, high-tensile steel were prepared for heat treatment and for Bulge Tests according to the procedures shown in Fig. 1 (top). The chemical analyses of the welds and plate were determined to be as follows:

Type	% *C*	% *Mn*	% *Si*	% *Ni*	% *Cr*	% *Mo*	% *V*	% *P*	% *S*
Mn-Mo (230) weld	0.11	1.92	0.36	0.07	0.09	0.21	0.07	0.014	0.019
Ni-Mo-V (260) weld	0.06	0.95	0.38	1.50	0.15	0.58	0.26	0.014	0.019
Plate	0.12	1.12	0.35	0.18	0.15	0.02	0.03	0.025	0.016

The method used to section the weldment to obtain blanks for heat treatment and for the subsequent machining of Charpy V specimens from the heat-treated blanks is shown in Fig. 1 (bottom). The position of the notch was established accurately at the center of the weld deposit by light etching of the heat-treated blanks. The notch was located near the crown of the weld to minimize dilution effects.

Initially, the specimens were heat treated in an accurately controlled electric muffle furnace, but the majority of the specimens were heat treated in lead baths. The change was made to provide more accurate timing of short cycle heat treatments and then retained for the remainder of the investigation. No appreciable differences resulted from the two types of heat treatment, as will be demonstrated by duplicate test data to be presented. In the case of the lead bath treatments, the specimens were immersed singly; a chromel-alumel thermocouple, flash welded to the initial blank, was used to record the time required to reach temperature and to check on the temperature drop in the lead bath as the result of the addition of subsequent specimens. After holding for the desired times the blanks were quenched in a brine solution in order to obtain rapid cooling through temperature ranges which possibly would develop embrittlement on slow cooling.

The Charpy specimens were tested in a standard testing machine; 30 specimens were used to determine the transition curves for the as-welded condition and 12 specimens were used for each of the heat-treatment series. The test temperatures were chosen so as to establish the low energy range in preference to the higher energy portions of the curves. This procedure was followed in order to establish the 15 ft-lb transition temperatures which were used as the criterion of relative notch toughness of the various weld conditions.

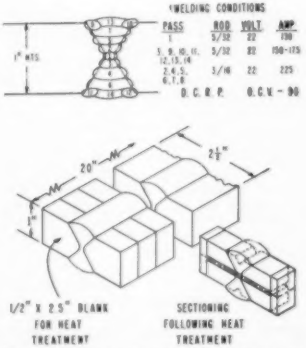


Fig. 1 Details of welding conditions and specimen preparation

RESULTS OF HEAT-TREATMENT STUDIES

The results of Charpy V and hardness tests for the various heat treatments of the Ni-Mo-V (260) deposit are summarized in Fig. 2. A log time plot is used to illustrate the consistency of the 15 ft-lb temperature transition and hardness changes. The 15 ft-lb transition data are also plotted on a conventional time scale in Fig. 3 to emphasize the nature of the temperature-time dependent changes. These plots indicate a typical precipitation reaction involving overaging; the hardness and notch-toughness changes are essentially parallel. In the temperature range of 850 to 1100° F, the rapidity and extent of embrittlement increases regularly with increased temperature; from a viewpoint of practical stress-relieving times the loss of notch toughness which is developed is not recoverable. In the range of 1150 to 1250° F, a rapid recovery of the initially rapid embrittlement is indicated within practical stress-relief times. At 1200° F the recovery is essentially complete after 24 to 48 hr; at 1250° F recovery is complete after 4 hr. However, continued holding results in second-stage embrittlement believed due to austenite formation. Dilatometric studies indicated that the Ae temperature of the weld deposit is approximately 1250° F which confirms the indications of slow austenization at 1250° F under conditions of prolonged holding deduced from the Charpy data. Figure 4 presents a graphic illustration of the time-temperature zones characterized by various degrees of embrittlement and recovery.

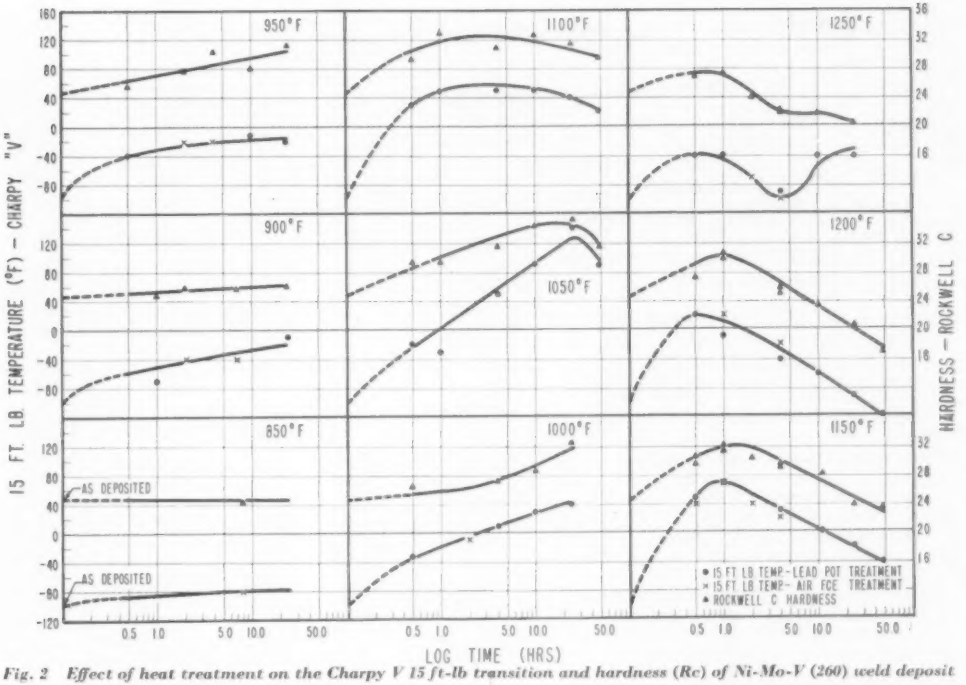


Fig. 2 Effect of heat treatment on the Charpy V 15 ft-lb transition and hardness (Rc) of Ni-Mo-V (260) weld deposit

The essentially complete recovery of notch toughness by a prolonged holding (48 hr) at 1200° F and by shorter holding times (4 hr) at 1250° F is not accompanied by a recovery in strength properties. This is indicated by the hardness plots and also by the tensile tests of the weld metal, as follows:

	Hardness, Rockwell C	*Tensile strength, psi*	*Yield strength, psi*	*Elongation, % in 2 in.*
As-deposited	24.5	120,000*	110,000*	18.0*
48 hr at 1200° F	17.0	94,250	82,500	24.0
4 hr at 1250° F	22.0	99,750	88,500	22.5

* minimum value (Mil. E986 Ships).

The results of similar tests on the Mn-Mo (230) deposit are presented in Table I. These data indicate that the as-welded notch toughness of the Mn-Mo (230) deposit is slightly less than that of the Ni-Mo-V deposit (15 ft-lb temperature −80° F as compared to −100° F) and that the hardness also is lower (23.0 Rc as compared to 24.5 Rc) as expected from the lower tensile strength of the Mn-Mo type. The effect of heat treatment is observed to be relatively slight. In the range of 900 to 1000° F there is relatively no significant change in notch toughness or hardness for times of 10 hr; a slight embrittlement is observed at 24 and 48 hr which probably represents a small sensitivity to temper embrittlement at the optimum temperatures for developing this condition.[8] In the range 1050 to 1250° F a significant increase in notch toughness and decrease in hardness are observed indicating tempering effects; the small decrease in notch toughness observed between 24 and 48 hr at 1250° F indicates slow austenite formation at this temperature. It may be concluded that in the range of conventional temperatures and times used for stress relieving the Mn-Mo (230) weld deposit develops a slight improvement in notch toughness with a concomitant small decrease in strength level.

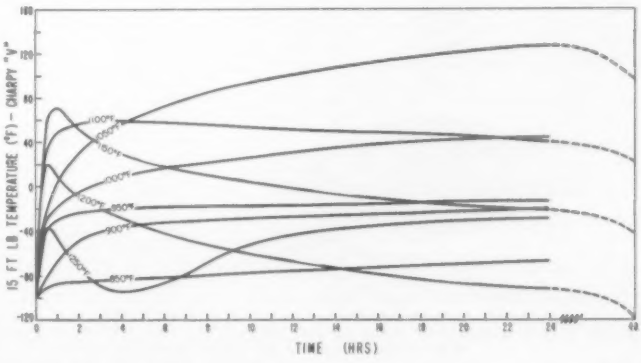


Fig. 3 Effect of holding time at various stress-relieving temperatures on the Charpy V 15 ft-lb transition for Ni-Mo-V (260) weld deposit

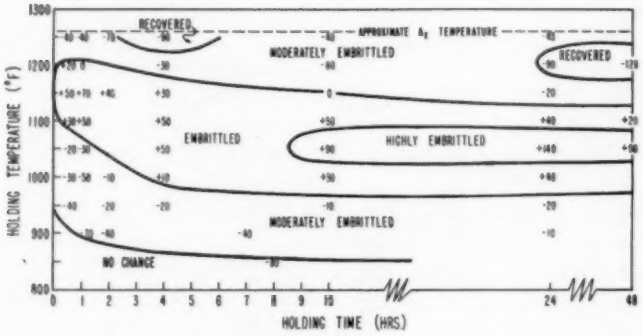


Fig. 4 Time-temperature zones of embrittlement and recovery for Ni-Mo-V (260) weld deposit. The values shown indicate Charpy V 15 ft-lb temperature transitions (° F) for respective holding times and temperatures

Table 1—Effect of Heat Treatment on Mn-Mo (230) Weld Deposit

15 ft-lb. temperature (° F)—Charpy "V"

Holding time, hr	*900° F*	*950° F*	*1000° F*	*1050° F*	*1100° F*	*1150° F*	*1200° F*	*1250° F*
½	..	..	−90	−100	−100	− 90	−100	−110
1	..	−70	−90	− 80	−110	−100	−100	−120
4	..	..	−70	− 60	− 90	−100	−100	..
10	−70	..	−90	..	− 90	−100	− 80	−120
24	−50	..	−70	..	− 90	−110	−100	−110
48	..	−50	−60	− 80	−100	−100	−100	− 70
As-deposited −80° F								
			Hardness,* Rockwell C					
½	..	..	23.0	23.0	22.0	21.0	22.5	21.5
1	..	23.0	23.0	23.5	21.5	22.0	22.0	22.0
4	..	..	23.5	24.0	22.5	19.5†	18.5†	22.0
10	23.0	..	23.0	..	21.0	18.0†	20.0†	19.5†
24	23.0	..	23.0	..	20.5	17.0†	14.5†	12.0†
48	..	23.0	23.5	21.5	21.5	19.0†	13.5†	80 Rb
As-deposited 23.0 Rc								

* Average of at least 4 readings.
† Converted from Rockwell B.

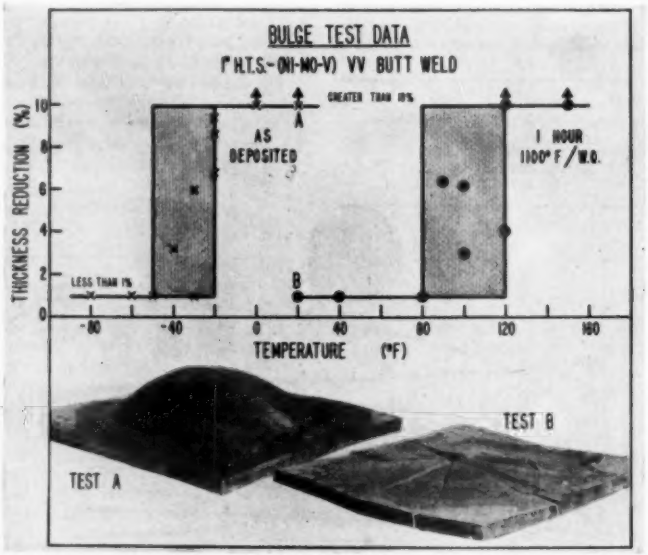


Fig. 5 Comparative bulge transition curves for as-deposited and stress-relieved Ni-Mo-V (260) weldments. Note extreme differences in the ability to develop deformation shown by the 20° F tests

BULGE TEST DATA

The Explosion Bulge Test method has been described previously.[3,4] Briefly, the method consists of developing a 15-in. diam. bulge in the test weldment (20 × 20-in. × plate thickness) to the point of failure; the bulge is developed in steps by a series of explosion loadings. The relative performance shown by a series of tests conducted over a range of temperatures is denoted by the level of prefracture deformation measured as the reduction of thickness of the plate at failure. Bulging is not carried above 10% thickness reduction because a full spherical shape is developed at this level and further deformation becomes localized at the peak of the bulge. Deformations of 1% or less are classed as 1% because of the difficulty of accurate thickness measurements below this level. Figure 5 presents comparative bulge transition data for as-welded and stress-relieved (1100°F/1 hr water quench) Ni-Mo-V (260) weldments of 1-in. high-tensile steel prepared with the same welding procedures which were used for the heat-treatment blanks. A marked difference in the ability to develop deformation with lowered temperature is indicated. The 10 to 1% transition occurs in the range of −50 to −20° F for the as-welded series and in the range of 80 to 120° F for the heat-treated series. The extreme difference in performance is illustrated by the 20° F test bulges shown at the bottom of Fig. 5. The unbroken bulge of the as-welded plate developed full 10% deformation after which the test was discontinued. The brittle fracture of the heat-treated plate is due to the brittle cracking characteristics of the as-rolled high-tensile steel (15 ft-lb Charpy V transition +30° F) at this temperature[5] and is of no significance to weld performance. This behavior merely indicates that once a failure is started, the plate is subject to brittle fracture. If the deformation of the unbroken bulge had been continued to failure a similar brittle fracture would occur.

These tests provide a further illustration of the controlling effect of weld notch toughness on the performance of weldments which was demonstrated in a previous report.[4] In this respect it should be noted that the performance differences are not related to strength differences, inasmuch as the increased hardness of the 1100° F stress-relieved weld (32 Rc as compared to 24.5 Rc) indicates higher strength for the weld which performs

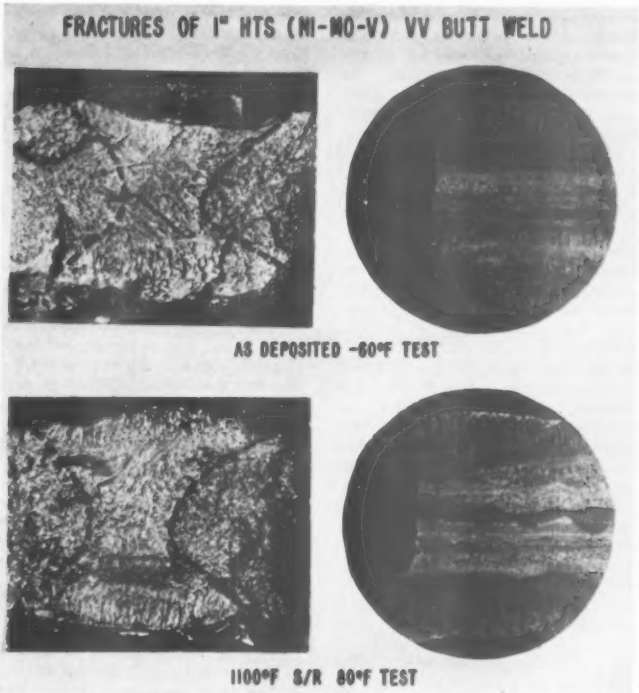
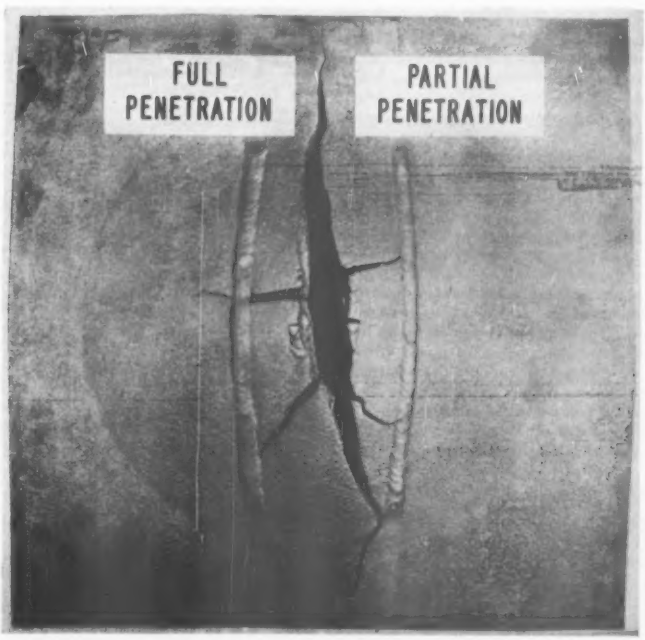


Fig. 6 Bulge test fractures (left) and cross section of fractures (right) of as-deposited and stress-relieved Ni-Mo-V (260) weldments. Note completely brittle fracture of the heat-treated weld as compared to heavy shearing of the as-deposited weld



poorly. Smith and Rinehart[1] indicate that an 1100° F treatment for 1 hr increases the tensile strength of the deposit from 120,000 psi to approximately 132,000 psi. It is obvious from this that a simple classification of tensile strength such as used in the AWS electrode codes does not properly classify the specialized high-tensile, notch-tough electrodes of this type.

It is interesting to note that the failures of the heat-treated weldments in all cases were initiated in the welds, as indicated by the chevron developed on the brittle fracture surfaces which pointed back into the welds proper. The failures of the as-welded weldments in all cases were initiated in the HAZ at the toe of the weld reinforcement. The shift in the site of the fracture origin is ascribed to the relative notch toughness differential between the as-deposited weld metal and the as-welded HAZ on the one hand and the heat-treated weld metal and the heat-treated HAZ on the other.

The relative notch toughness differential between the as-welded and heat-treated welds (15 ft-lb temperature −100 and +50° F, respectively) is manifested not only in the bulge deformation differences and shifts in the fracture origin but also in the fracture appearance of the welds. Figure 6 illustrates the completely brittle fracture appearance of the heat-treated weld in contrast to the heavy surface shearing developed by the as-deposited weld, despite the lower test temperature of the latter fracture. The development of surface shear on the fracture of the as-deposited weld is indicative of a higher level of notch toughness; this subject has been discussed in detail by the authors.[8]

The significance of surface shear to the problem of the initiation and propagation of failure in weldments is demonstrated by the ability of as-deposited Ni-Mo-V (260) welds (VV Butt) to stop the propagation of brittle cracks at normal service temperatures. Figure 7 illustrates a bulge crack-starter test[5] featuring the starting of a brittle crack in a mild steel ship plate by means of a short bead-on-plate weld of a brittle hard surfacing electrode. The brittle cracks radiate outward across the mild steel plate which does not develop surface shearing and accordingly is capable of brittle fracture at +70° F; however, the fractures terminate on impingement with the highly notch-tough weld deposit. The termination is complete in the case of the partial penetration weld because the 1/8-in. land at the center served as an internal crack arrestor aiding the surface shearing of the weld in stopping the crack.

Fig. 7 Crack stoppage by as-deposited Ni-Mo-V (260) VV butt welds in 1-in. ship plate; crack starter test at 70° F

The full penetration weld depended entirely on the surface shearing of the weld and was not completely effective as denoted by the slight crack at the other side of this weld. The refusal of the weld to develop complete brittle fracture to the surface prevented the propagation of the crack which had advanced through the centerline of the weld and then resurfaced on the plate side. A potential use of such welds as crack arrestors is indicated by the subject tests. It is apparent that the high notch toughness of the weld rather than its high strength is responsible for this performance.

GENERAL DISCUSSION

The results of this investigation demonstrate that the mechanism of embrittlement of Ni-Mo-V (260) weld metal in the range of stress-relieving temperatures is due to a precipitation reaction typical of "age-hardening" systems. The Mn-Mo (230) weld metal is relatively unaffected by stress-relief heat treatment. Extensive prior research by various investigators[4-9] has established that vanadium steels develop "secondary hardening" effects which are concomitant with embrittlement. Molybdenum steels on the other hand are known to develop similar effects only at considerably higher alloy contents than present in the subject weld metal. It is concluded accordingly that vanadium carbide precipitation is responsible for the embrittlement effects. The nature of the reaction, both as to its rapidity and recovery at the same temperature, distinctly differentiates the embrittlement from the relatively slower and isothermally nonrecoverable embrittlement classed as temper brittleness.[3]

Zimmerman, Aborn and Bain[6] have provided a well-substantiated description of the vanadium precipitation mechanism which serves to explain the kinetics and reversibility of the reaction in the stress-relieving temperature range and also the failure to develop embrittlement on initial cooling during welding. It is deduced, according to this mechanism, that vanadium carbide precipitation does not occur during the initial cooling because of the mass action effect of the preponderance of iron atoms relative to the vanadium atoms, which restricts carbide formation to the iron-rich carbide in a bainitic distribution. Thus, the as-deposited weld metal is characterized by a microstructure of high notch toughness and by the distribution of vanadium in solid solution in the ferritic matrix. On heating, the carbon in solid solution attains sufficient mobility to become available to the essentially fixed vanadium atoms, resulting in a finely dispersed vanadium carbide precipitation. As the carbon is withdrawn from solid solution by formation of vanadium carbide, a gradual solution of the less stable iron carbide occurs to replenish the carbon-depleted ferrite matrix. Thus, a continuous process based on the difference in solid solubility of the carbides results in replacing the iron carbide to the limit of the availability of vanadium. The rate of vanadium carbide precipitation is controlled primarily by the mobility of the carbon atoms; the reaction rate which is relatively slow at 800 to 950° F increases rapidly with increasing temperatures such that essential completion in a period of approximately 1 hr at 1150 to 1250° F is indicated by the present studies. At these high temperatures the increased solubility and diffusivity of the vanadium carbide results in rapid coalescence of the initially finely dispersed carbide particles, hence in "overaging," within practical stress-relieving times. As the result, the embrittlement developed at the lower temperatures is, from a practical viewpoint, essentially permanent, while at the upper temperatures notch toughness is recoverable within practical heat-treating times. The limiting temperature for oversaging with consequent recovery is the equilibrium (Ae) temperature which results in austenite formation and consequent damage with prolonged holding. The nature of the damage, whether by austenite transformation to poor quality microstructures during cooling or by iron carbide precipitation in semicontinuous fashion at the grain boundaries, was not established. The continued decrease in hardness observed during the second-stage embrittlement period suggests that the process entails grain boundary precipitation.

The foregoing metallurgical considerations are of considerable practical significance to the problem of stress relieving of weldments. It is apparent that Mn-Mo (230) weldments are essentially foolproof from a notch-toughness standpoint. The higher strength Ni-Mo-V (260) weldments present a difficult case for stress relieving heat treatment; the high notch toughness, which is of primary importance to this class, is lost by conventional stress-relieving practice. The extent of embrittlement which is developed is much more extreme than has been suspected. The essentially complete recovery obtained by prolonged, but practical, stress-relieving times at 1200° F and by shorter heating times at 1250° F provides a possibility for stress-relieving treatments of critical weldments requiring the use of the Ni-Mo-V (260) weld and for the recovery of such weldments accidentally damaged by improper treatment in the range of 1000 to 1100° F. The recovered weld deposit, however, does not meet the tensile and yield strength requirements of the subject specification (Mil. E986—Ships).

The foregoing metallurgical considerations also indicate that the vanadium content of the weld deposit is critical and that it should be expected that deposits entailing vanadium contents on the high side of the range should be more difficult to handle than similar deposits of lower vanadium contents. In this respect the extent of the dilution obtained by procedure variations should be expected to have a contributing effect. It is indicated that very high preheats which sometimes are used for massive sections, or high interpass temperatures, which may be developed in welding thin sections, may serve to provide sufficiently slow cooling rates in the embrittling range to result in precipitation effects.

ACKNOWLEDGMENTS

The active interest and support in the establishing and conducting of this investigation by Dr. O. T. Marzke, Superintendent of the Metallurgy Division, and the many contributions of E. Eschbacher and J. Davenport to the experimental phase of the investigation are gratefully acknowledged.

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Studies of the Crack Sensitivity of Aircraft Steels

Factors influencing the crack sensitivity of several aircraft steels and interrelated phenomena of the mechanism of microcracking

by A. W. Steinberger and J. Stoop

INTRODUCTION

THE necessity for maintaining the highest possible weld quality in the fabrication of highly stressed aircraft components has dictated a continuing study of the factors effecting the integrity of welds in alloy steels. One of the major conditions which must be controlled is the formation of actual or potential microcracks or embrittlement which might eventually lead to failures of the associated structure.

For the past several years the authors have conducted investigations of the factors influencing the crack sensitivity of several aircraft steels. Certain phases of these studies were described in a paper[1] presented at the Thirty-First Annual Meeting of the AMERICAN WELDING SOCIETY.

The present paper deals with a continuation of the previous work and includes a more detailed study of the interrelated phenomena which enter into the mechanism of microcracking. It is, of course, evident that the factors described are not by any means all that are involved and the data are presented simply as additional segments in hopes that they may eventually be fitted together and that a complete picture will emerge which will enable the subject to be dealt with as a science rather than an art.

Among the aspects investigated were:

(*a*) The microstructures associated with microcracking.

(*b*) The effect of superimposed stresses upon the threshold value of hydrogen necessary to induce microcracking.

(*c*) The influence of the magnitude and direction of superimposed stresses upon the nature and orientation of microcracks.

(*d*) A study of methods of specimen examination to determine those best capable of revealing the presence of microcracks. This was for the purpose of determining whether previous methods of examination revealed all of the microcracks present, particularly those of small size.

(*e*) A study of specimens welded with low-hydrogen processes at very high quenching rates to determine the relative crack sensitivity of the various processes and to establish whether cracking could be induced.

(*f*) An effort to determine whether the high aerostatic pressures which cause microcracks were due to the rejection of hydrogen during the formation of martensite, or to the diffusion of hydrogen to areas in which it could change to the molecular state.

In these studies we have drawn freely upon the valuable data presented in the excellent papers by Flanigan and Kaufman,[10] Bland,[7] Rollason[8] and Roberts,[3] Hoyt, Sims and Banta,[9] D. P. Smith,[2] Carl Zapffe[4] and others.

HYDROGEN THEORY

The theories of the mechanism of hydrogen embrittlement and cracking together with descriptions of the manner in which hydrogen is introduced and contained in the metal have been ascribed by various investigators to causes in line with their particular field of study. This information has appeared so frequently in the literature that it may seem redundant to again review the subject. In the light of continued studies, however, it would appear timely to bring together some of the various segments of knowledge which have been contributed by specialists in the field, and attempt to fit them together to form a more closely integrated picture of these phenomena.

TRANSFORMATION CRACKING

Cold cracking (or hydrogen cracking) is a very general term for a fissure-like defect, and it is surprising to see the number of basic causes for this form of cracking. An interesting observation regarding cold cracking is that such cracks need not form immediately after processing. In fact, many cases of cracking develop after the part has been in service for a period of time. Because of the many possible combinations of temperature, stress, rate of loading, corrosive environment and so forth that may exist in service, the reasons for failures or cracking are often very difficult to determine. Some authorities believe that a major factor influencing the cracking of weldments and the heat-affected zone is the tremendous local pressure set up when hydrogen occluded in austenite is released during transformation at ordinary temperatures.[6] The hydrogen theory of cold cracking is now widely accepted, although the exact mechanism of crack formation is not completely understood. Many investigators have contributed to this problem and some of the factors determined are hereby summarized.

Although steel is impermeable to molecular hydrogen, it exhibits a variable permeability for atomic hydrogen. During the welding operation hydrogen or hydrogen-bearing compounds such as moisture (originating in the welding gases, electrode coatings, flux or the surrounding air) are dissociated in the arc releasing atomic hydrogen).[2] This is occluded in the molten weld metal. The important fact to recognize is that molten iron or steel can dissolve a much larger quantity of hydrogen than they are capable of holding in the solid state. The capacity to hold hydrogen decreases rapidly with de-

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creasing temperature and sharp changes occur at the transformation points.[3]

As the temperature of the molten metal falls, part of the hydrogen will, of course, diffuse into the air and also diffuse into that portion of the heat-affected zone[3] which is heated above the transformation point.[2] During this cooling period the diffusion rate of the hydrogen through the metal is decreasing.[5] Actually the fall in temperature is normally so rapid that diffusion cannot take place rapidly enough to maintain equilibrium conditions and the greater part of the hydrogen is trapped in the solidified austenitic metal thus forming a "solid solution" in steel. As the solid metal cools, the state of solid solution becomes one of greater "supersaturation." At some point in the process the hydrogen changes from the atomic to the molecular state with a consequent further increase in pressure.[3] The molecular form of hydrogen is insoluble in steel and is believed incapable of diffusing through the metal.[2]

Hydrogen is evidently soluble in austenite at all temperatures. It is, however, believed by some investigators to be practically insoluble in cold ferrite or martensite[6] and during cooling, therefore, it is rejected from all areas except those which remain austenitic. As cooling continues the diffusion rate of the hydrogen through the metal is constantly decreasing. If transformation is completed at sufficiently elevated temperatures, the hydrogen is still mobile and can diffuse through the steel and escape without causing any damage.[1, 3, 5] It has been found that transformation cracks are never produced at temperatures above 600° F.* In the case of steels having a delayed transformation characteristic however, a portion of the austenite will not transform until the temperature has fallen well below the point at which the hydrogen can still diffuse through the steel. In these areas of austenite, the hydrogen concentrates to relatively high values. When the austenite later transforms at room temperature, it is rejected with no place to go and enormous aerostatic pressures (estimated at several million pounds per square inch) are set up which disrupt the adjacent structure, even though it be the hardest martensite.[6] The cracks so formed are undoubtedly quite small and the principal function of thermal stresses, and the volume changes accompanying the formation of martensite, may be to cause these cracks to grow to visible size. This growth of crack size with time has been frequently observed[1] and is no doubt often responsible for cracks which show up on components, even though previous magnetic and radiographic inspections revealed no indications.

In the previous paper, arrested cooling and postheating were shown to be effective in preventing the formation of microcracks even for welds containing very high hydrogen levels. Efforts to employ isothermal transformation diagrams for the purpose of establishing the optimum time and temperature for such thermal treatment were not always successful. While such diagrams were found useful for establishing approximate working values it was necessary to determine the final conditions by experimental procedures. This was not unexpected and is readily explained. In the first place the available diagrams do not allow for the range of austenitizing temperatures or the very brief time at that temperature encountered in welding.

It is also well known that it is unusual for wrought steel to be free of segregations. This results in local areas of a different composition than the surrounding matrix. Such local areas may not respond to heat treatment in the same manner that the rest of the steel does. As a consequence such areas may not transform during the normal postheating procedures which are intended to cause transformation to occur at a temperature sufficiently elevated that the hydrogen is mobile and can escape as it is rejected during this period. As a consequence these local areas may undergo delayed transformation at a very much later time and the consequent rejected hydrogen, no longer being mobile, might introduce microcracks which could subsequently enlarge in service. Such delayed transformation might also not occur until the part had been in service for a considerable period of time.

COLD CRACKING DUE TO HYDROGEN DIFFUSION INTO CAVITIES AND TRANSFORMING TO MOLECULAR HYDROGEN

Cold cracking in steel containing hydrogen may also occur by other means. The occluded atomic hydrogen is believed to diffuse through the steel until it encounters some internal cavity. By cavity, we imply gas pockets, voids around nonmetallic inclusions, or even discontinuities or rifts of very small order in the crystalline structure of the metal (such cavities of submicroscopic size are believed to be quite common in all metals). Diffusion of hydrogen into these rifts occurs at an accelerated rate through metal which is under stress and is proportional to the square of the pressure difference. Upon diffusing into an internal cavity in the metal of molecular size or larger, the hydrogen is presumed to change from the atomic to the molecular form. Diffusion into the cavity will continue until the pressure of the molecular hydrogen is equal to the square of the pressure of the "dissolved" atomic hydrogen. Exceedingly high pressures may thus be built up in the cavity before equilibrium is attained.[2] Should the cavities contain oxide inclusions the hydrogen may also react to form water vapor.

It is possible that the internal stresses will not always rise to the threshold of cracking as the weldment cools to room temperature so that the metal remains free of cracks. The additional stress needed to produce a crack may be applied in service.[2]

With these facts in mind it may be reasoned that the rejection of hydrogen during martensite formation may not necessarily be the instrument through which microcracking occurs. It may also be that delayed transformation of austenite is not the explanation of the cracks which have been observed to appear after periods ranging up to days or even weeks subsequent to the completion of welding. Could it not be that these cracks are due to the slow diffusion of hydrogen and the cracking mechanism mentioned above?

As postulated by Smith[2] the hydrogen may diffuse through rifts of atomic order. During martensite formation a dimensional change occurs which subjects the structure to microstresses which may increase the size of some of the rifts. (Stresses due to shrinkage and restraint could act in a similar manner.) As the hydrogen diffuses into these rifts of molecular size or greater, it transforms to the molecular state and its pressure increases as the square of the initial atomic hydrogen pressure. Zapffe[4] has shown that this initial pressure could equal hundreds or even thousands of atmospheres. It is easily realized that pressures can readily reach values sufficient to cause the observed cracking.

With regard to delayed cracking, anyone who has submerged a weld specimen in a liquid such as glycerin has observed that the hydrogen bubbles often continue to emerge from the rift mouths for periods of several days. This would imply a slow diffusion of the hydrogen through the steel. As a consequence some time may be required before sufficient hydrogen diffuses into the voids to cause cracking. This could explain the delayed action cracking often observed and suggests that delayed transformation of austenite may not be responsible for this phenomenon. Obviously, further investigations will be required to determine the exact mechanism of microcracking.

HYDOGEN EMBRITTLEMENT

When hydrogen is held in the crystalline structure the steel may behave in an embrittled fashion. Embrittlement has been defined as the tendency for the metal to display abnormally low ductility for its existing structure and hardness. The mechanism by which hydrogen embrittles steel is not definitely known. Of the hypotheses that have been advanced on hydrogen embrittlement the following seems to be the most likely explanation. Briefly when the amount of hydrogen exceeds the solubility limit and a condition of supersaturation is in force, the hydrogen

* The authors have found exceptions to this statement which will be described.

which cannot escape by the avenues described elsewhere in this report, seeks to precipitate in the crystalline lattice. It is suspected that the crystalline lattice of many metals contain a large number of minor imperfections which might be regarded as submicroscopic voids.[2,3] Minute amounts of hydrogen precipitate into these spaces or rifts. Upon precipitating the hydrogen changes to the molecular from and because of the square root relationship between the pressure of the atomic hydrogen and the precipitated molecular hydrogen, tremendous pressures are built up in these rifts. *It may be possible that these rifts do not exist in the metal before application of a load but that they form during deformation.* The mechanism of embrittlement therefore, would be one of straining to form (molecular sized) rifts followed immediately by diffusion of hydrogen to precipitate in the rifts. *The embrittlement may thus be caused by pressure of molecular hydrogen under very high pressure (just below the threshold of cracking) in submicroscopic rifts in the metal.*

The degree of embrittlement in a given steel and at a given stress level appears to depend upon the amount of hydrogen occluded. The extent of embrittlement may vary markedly from one grade of steel to another.

Increasing carbon content results in increasing susceptibility to embrittlement. Steels having a face-centered cubic type of crystalline lattice such as the austenitic steels, show little tendency toward embrittlement.[2]

Although low-carbon steels are not especially sensitive to general embrittlement to "dissolved" hydrogen they are prone to develop a peculiar form of localized embrittlement commonly referred to as fisheyes (or flakes) which have a marked detrimental effect upon the ductility of the metal. Fisheyes appear as small spots[2] on the gray fractured surfaces of broken specimens of weld metals. The fisheyes usually surround some kind of discontinuity in the metal such as a gas pocket or nonmetallic inclusion which accounts for the appearance of the eye.

Fisheyes (or flakes) do not necessarily represent an area in the metal which was cracked prior to fracture of the part to test or in service. Instead, it may be a portion of the metal which prematurely failed in an extremely brittle manner and in so doing promoted the failure of the metal as a whole.

Apparently, when atomic hydrogen precipitates in a (microscopic) pocket or void to form molecular hydrogen, the pressure can be sufficiently great to cause straining in the crystalline lattice immediately surrounding the cavity. This means, of course, that submicroscopic rifts are created in the lattice, if they are not already there because of residual stress concentration around the cavity. Next, the hydrogen will diffuse to this locality and precipitate in these rifts.[2] Here again we have molecular hydrogen under high pressure in rifts, but now only in a localized area. Here the embrittlement will be localized.[2] Some investigators report that fisheyes can be prevented by heating the metal to some temperature in the range of 200 to 1300° F while others report that this does not correct the condition in all cases. (Further study is required.) Perhaps this discrepancy depends upon whether the microcracking threshold pressure has been exceeded. Fisheyes and hydrogen flakes may well be different manifestations of the same phenomena. The differences in appearance may be due to the amount of hydrogen and the direction, magnitude and rate of application of the stress.

Smith explains the part played by occluded hydrogen as follows: Brittleness is caused by the portions of hydrogen which are last to be occluded and which first escape, either at ordinary or at elevated temperatures. It is simplest to suppose that these portions produce an excess of internal pressure and a distension of the rifts which render the metal weak, in respect at least to shearing stresses of certain kinds. The rather scanty information available appears to indicate that embrittlement has a directional quality.[2]

METHOD OF INTRODUCING HYDROGEN INTO METALS

The phenomena governing the manner in which hydrogen is introduced and contained in metal again appears to be influenced by several factors:

1. Trouble from hydrogen is common in aluminum and magnesium, periodic in iron and steel and rare in nickel.[4]

2. Under suitable conditions hydrogen is occluded in amounts much in excess of its solubility. Such surcharging may occur when metal is exposed to gaseous hydrogen (or moisture) in its cold worked condition, when metal, even in its compact form, is subjected to cathodic, ionized or chemically liberated hydrogen.

3. There is finally the possibility that metal which has been saturated at a high temperature may retain extra hydrogen above the solubility limit. This has been shown to actually occur.[2]

Zapffe has shown that in the ferrous industry the role of moisture as a hydrogenizer has been widely indicated though never well understood.[4]

To moisture can be traced the fact that an improperly dried charge ladle lining or mold can lead to hydrogen causing gasiness, that weld defects from moisture in an arc-weld electrode coating can be more severe than those from the atmosphere of atomic hydrogen itself, that hydrogen-caused defects are more prevalent during the summer months (or in humid climates,[4]) that southern pig iron differs from northern pig iron in at least one respect traceable to hydrogen.

In addition there are the phenomena of wild heats, porosity and low ductility associated with additions of deoxidizer elements, aluminum, silicon, manganese, also chromium in stainless steels which become readily explained when the individual steam-metal reactions are explored thermodynamically on the basis of their respective *hydrogen potentials.*[4]

Over almost the entire temperature range, one atmosphere of steam has at least the hydrogenizing potential, even on oxygen-saturated iron, as one atmosphere of pure hydrogen gas. With increasing temperature, steam becomes an even more virulent source of hydrogen. Where the oxygen pressure is less than saturation the hydrogen potential becomes much greater.[4]

Error has developed from attempting to relate hydrogen solubility into the PH_2 of the external system. Instead, one must consider that when moisture, H_2O, is allowed to react with a metal, the hydrogen which is released has a hydrogen potential which depends upon the thermodynamic attributes of each particular system and can be calculated from recorded thermodynamic measurements. This "hydrogen potential" is that pressure of hydrogen which would be generated upon a closed system if the reaction proceeded to equilibrium without change in PH_2O.

Upon the basis of these calculations of hydrogen potential, several important metallurgical phenomena became clarified:

1. Iron develops hydrogen potentials approximately equal to the steam pressure when the iron is saturated with oxygen. At lesser oxygen pressures the hydrogen potentials increase so rapidly that cast irons, deoxidized steels and certain alloy steels[4] may develop conditions favoring hydrogen potentials of thousands of atmospheres.

2. In view of the great effect of the oxygen content upon the hydrogen potential of iron, one's attention immediately turns to the individual effects of those alloy elements in the steel which are strong deoxidizers.

3. Carbon is one of the alloy additions and the hydrogen potentials of carbon steels carry them into a class where the liability to hydrogen pickup from moisture must be increasingly guarded against.

4. Chromium and manganese increase the hydrogen potentials of steel into the hundreds of atmospheres of PH_2 and may suppress its appearance until some later period.

5. Silicon increases the hydrogen potential into the thousands of atmospheres while aluminum raises the hydrogen potentials into the millions of atmospheres of H_2.

6. It is significant that aluminum is notoriously liable to pinhole porosity from hydrogen pickup through the moisture reaction whereas under one atmosphere of pure hydrogen scarcely measurable amounts of gas are dissolved.[4]

7. Nickel masks the hydrogen activity

(as does manganese) by increasing the retentivity of the solid steel for hydrogen.

8. Air, even when relatively dry, may hydrogenize steel as effectively as melting under one atmosphere of pure H_2, and *the lower the oxygen pressure of the metal the greater the hydrogen potential becomes. Increasing humidity rapidly increases the hydrogenizing power of the air to an equivalent of many atmospheres of pressure of pure* H_2. Hot humid weather could quickly extend the hydrogenizing potential of steel containing 0.01 weight percent of FeO up toward many atmospheres.[4] Ionization, as during a thunderstorm, may make the influence of such moist air even more potent.

9. Iron and steel merely reacting at ordinary temperatures with moisture could charge themselves with sufficient hydrogen to account for numerous problems. These include the peeling of paint and similar defects in subsequently applied coatings.[4] Such charging with hydrogen may be sufficient to cause embrittlement of the metal even at normal temperatures. This is exemplified by the fact that *the main springs of watches tend to break more in summer weather and particularly during thunderstorms. The remarkable sensitivity of most spring steel to hydrogen embrittlement now provides an explanation for a previously puzzling phenomenon.*[4]

Such expressions as solid solution, dissolved, chemical combination, etc., should be used with regard to metal hydrogen systems only with full awareness of their sometimes misleading implications.

Thus, solid solution is a convenient term for iron-hydrogen which shows continuously variable properties within certain ranges of concentration. Smith claims that the treatment of this as homogeneous phases is subject to the objection that it assumes interstitial distribution of the solute together with its free diffusion in all directions through the metal lattice and consequent absence of discontinuities in the concentration gradients, all of them at variance with evidence assembled by D. P. Smith and others.[3] Iron hydrogen is more probably to be regarded a di-phasal system made up of a sound metallic lattice, unimpregnated with hydrogen, into which are thrust many "leaves" of the external gas phase.[3]

It is highly probable that the large quantities of hydrogen in surcharged systems are not contained interstitially in supersaturated solid solution, but that the hydrogen is contained in occasional enlargements or rifts in certain of the lattice spacings.[3]

Smith gives a great deal of supporting evidence to this theory by showing that the behavior of the hydrogen, X-ray diffraction results and other phenomena conform to this hypothesis and not to purely interstitial occlusion. For example:

1. The occlusive inertness of iron and certain other metals after prolonged high-temperature annealing appears to be due to the closing up of the structure or rifts in the metal, particularly in the outer layers where the surface tension cooperates during the anneal.

2. Iron rendered inert by annealing at once recovers its ability to occlude (hydrogen) in quantity upon being moderately cold worked.[3]

3. Diffusion occurs at an accelerated rate through metal under stress.[3] (By inference a strained condition causes a disarray of the lattice accompanied by a widening of occasional spacings of rifts. This, in turn, would make the metal sensitive to hydrogen pickup.)

4. The very large effect that is exerted upon the occlusive permeability by deformation of the metal indicates that slip at least increases openness of structure. In severely deformed metals the rifts are particularly numerous or particularly large in the vicinities of the slip planes. It is probable that the states of the metallic crystals are between a limiting condition in which they are unrifted and continuous and the condition in which the rift network, instead of the lattice, has become continuous.[3]

Regarding the magnitudes of the rifts it seems clear that these may vary from something closely approaching the normal lattice spacing of the metal to dimensions such that the cohesion of the lattice fails and the rift becomes a gross fissure.[3]

HYDROGEN OCCLUSION IN ALUMINUM

The known propensity of both aluminum and magnesium for hydrogen pickup from water vapor makes calculations of hydrogen potentials for these two metals of wide significance. The elemental metal in each of the cases comprises an industry in its own right and an industry notably troubled by a hydrogen-caused defect, pinhole porosity.[4]

Solubility of hydrogen in molten alu-

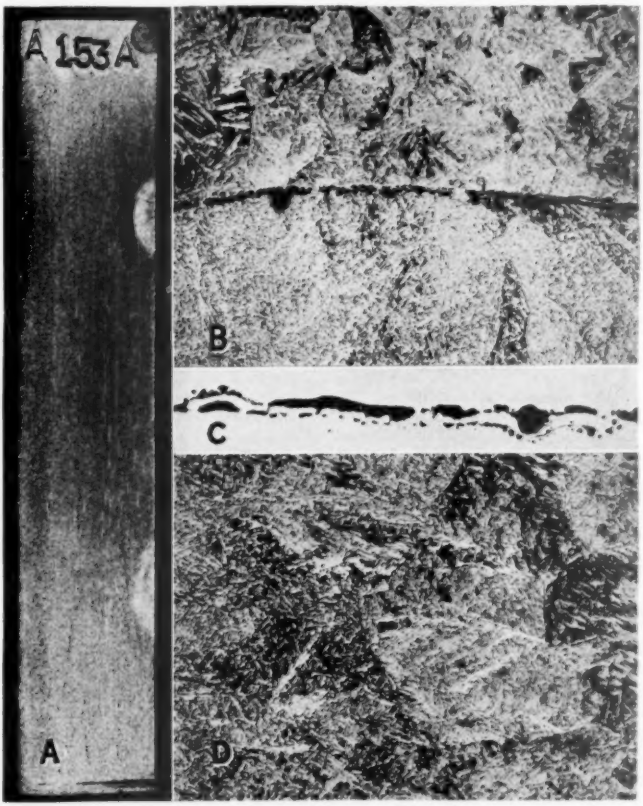


Fig. 1 Atomic-hydrogen-welded specimen, A-153A, using Type 4330 filler rod

Cooling arrested and postheating conducted at 600° F for 30 min. Underbead indication is shown to be an inclusion, not a crack. *A*—2½ ×, *B*—80 ×, 2% Nital; *C*—370 ×, *D*—80 ×, 2% Nital.

minum is fully established by the results of Bircumshaw 1926–35; of P. Roentgen, with Braun & Holler 1932–34 and others.[3] Aluminum develops hydrogen potentials from the moisture reaction which increase with decreasing temperature such that in its own melting range the hydrogen potentials reach a calculated value of 10^{14} atmospheres at the melting point. Such figures in a sense fantastic, compare well with Nernst pressures for the hydrogen potentials of aqueous pickling reactions. They essentially designate moisture as the unique source of hydrogen for molten aluminum whereas under one atmosphere of pure hydrogen scarcely measureable amounts of the gas are dissolved.[4]

Nothing is known from which any interstitial occlusion (solid solution) may reasonably be inferred while the slow difficult escape of hydrogen on heating the solid[1] metal *in vacuo* makes it highly probable that *diffusion in aluminum is purely a rift phenomena.*[3]

The use of aluminum as a deoxidizer for steel makes these observations significant in any study of the influence of hydrogen upon welded fabrications.

PROCEDURES

Generally speaking the procedures used in making these studies were similar to those described in the previous paper.[1] Except where otherwise stated the following conditions apply:

1. The same form of circular weld bead specimens were employed.

2. Specimens were welded without preheat.

3. Deposits were made upon modified Type 4320 plate material.

4. After welding was completed the specimens were placed in an air-cooling jig. This consisted of a locating nest positioned under an air nozzle having a fixed size and distance above the specimens. The nozzle contained a diffuser cone for the purpose of obtaining a uniform distribution of air over the specimen. Air pressure and flow rates were standardized as described in Reference 1. Cooling was continued to the desired postheating or investigating temperature after which the specimens were immediately placed in the postheating furnace or quenched as required. After cooling to room temperature, specimens were held at 40° F for 24 hr to perm.t completion of cracking, if any.

5. Following this they were stress relieved for 1 hr at 1060° F and then were surface ground under coolant to a depth of 0.100 in. below the original surface of the piece.

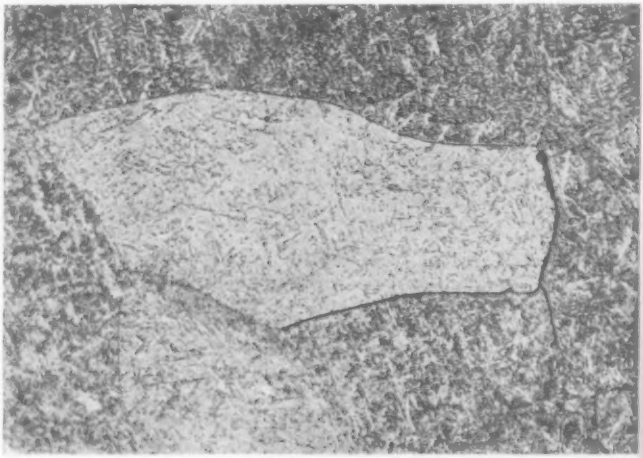
6. The indications were usually observed by means of magnetic particle inspection and photographed.

7. Certain specimens were rapidly cooled while welding by means of a fixture which permitted the lower 1/8 in. of the specimen to be immersed in a water tank while a jet of water impinged against its under side. In some instances the water level was lowered so that no immersion resulted and only the jet was employed either during or after welding. The steam evolved in this apparatus rose about the specimen with results to be described elsewhere.

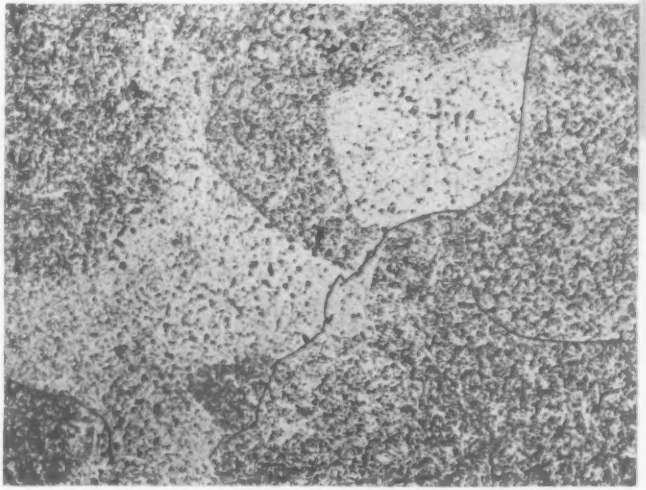
8. In other tests the tank and fixture were closed by a heavy steel plate top. The specimen was bolted over an opening in this top and sealed by means of suitable gaskets so that no water or steam could reach the welding zone and disturb the low-hydrogen characteristics of the welding processes employed. With this latter arrangement the test variable was limited to the cooling rate only and the disturbing influence of the water and steam as a second variable was eliminated.

9. As will be later described, certain butt-welded specimens were welded in a shallow pan containing sufficient water to immerse the lower 1/8 in. of the specimens.

10. Where high lateral restraint was to be imposed, this pan was made of heavy plate to which the edges of the specimen plates were welded.



(*A*) Intercrystalline cracking. × 100. Etched with zephiran chloride and picric acid in ether.



(*B*) Intercrystalline and transcrystalline cracking in area adjacent to above. × 100. Etched in zephiran chloride and picric acid in ether.

Fig. 2 Atomic-hydrogen welded with modified Type 6130 filler rod. Air-blast cooled to 200° F and water quenched. No postheat. Sample No. A-292

METHODS OF EXAMINATION

The circular weld bead specimens were normally examined for hydrogen cracks by means of magnetic particle inspection. It was previously explained that the specimens were ground to 0.100 in. below the original surface. Carbon tetrachloride was the fluid used for suspension of the magnetic particles instead of the light oil normally used for this purpose. Better photographs resulted from this procedure. Instances were observed in which magnetic particle inspection or microexamination followed by mechanical polishing and etching, did not reveal microcracking. This was particularly true with mild steels. In such instances electropolishing as described by Flanigan[10] showed cracks which were otherwise not visible. Mechanical polishing had a tendency to drag metal over the cracks thus obscuring them. Electropolishing opened up the cracks.

The solution used for our electropolishing procedure was developed by Sidney Weisman of our Metallurgical Staff. The composition of the Weisman solution and the techniques employed are outlined herewith.

Chromium Trioxide (CrO_3).....	25 gm
Water (H_2O).....	50 cc
Acetic Acid, glacial (CH_3COOH)	200 cc
Phosphoric Acid, 85% (H_3PO_4).	600 cc
Sulphuric Acid (H_2SO_4).....	150 cc

The ingredients were added in the order given. Stainless-steel cathodes were employed and positioned 1¼ in. from the work. For the 2- x 2-in. specimens the solution was held at 110° F by water cooling. The current density was approximately 2.2 amp per square inch. For small specimens of about ¾ in. square the temperature was about 85–90° F and the current density 5.3 amp per square inch. The voltage range was from 5 to 10 v.

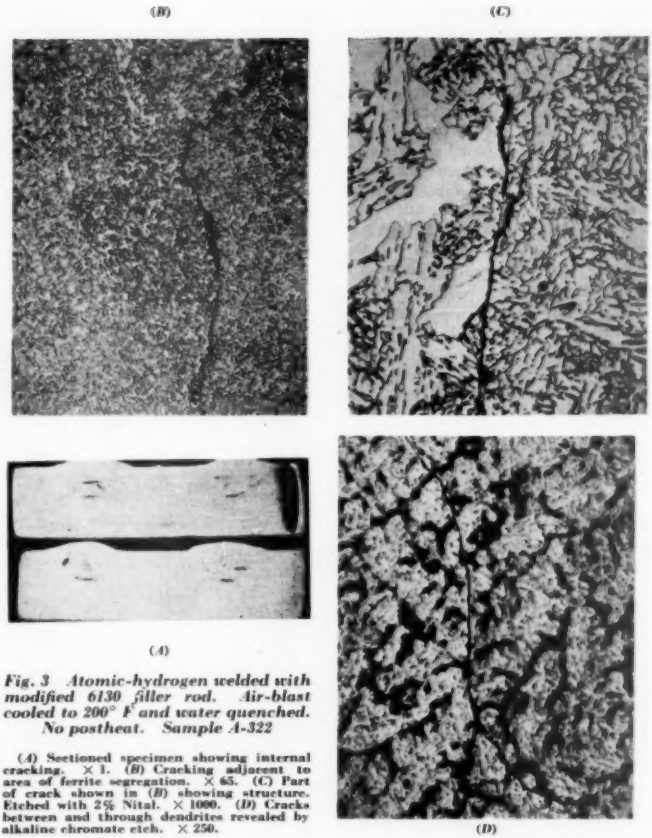


Fig. 3 Atomic-hydrogen welded with modified 6130 filler rod. Air-blast cooled to 200° F and water quenched. No postheat. Sample A-322

(A) Sectioned specimen showing internal cracking. × 1. (B) Cracking adjacent to area of ferrite segregation. × 65. (C) Part of crack shown in (B) showing structure. Etched with 2% Nital. × 1000. (D) Cracks between and through dendrites revealed by alkaline chromate etch. × 250.

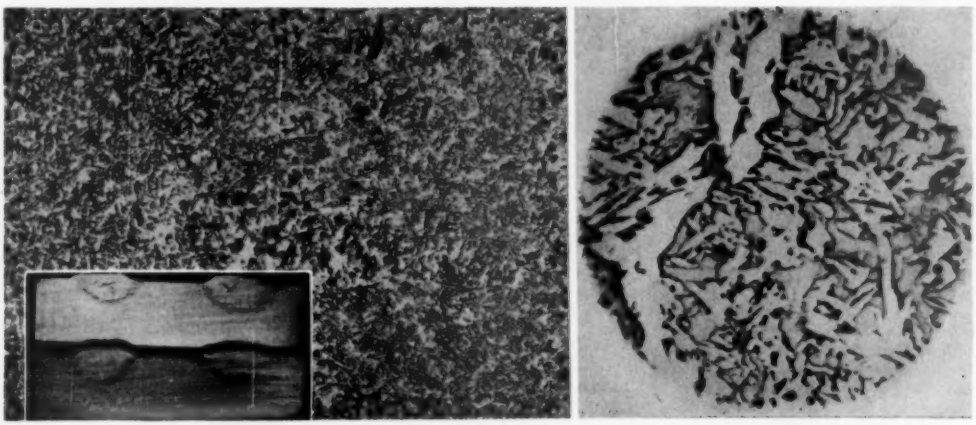


Fig. 4 Atomic-hydrogen welded with modified 6130 filler rod. Air-blast cooled to 950° F and water quenched. No postheat. Sample No. A-321

(A) Sectioned specimen showing internal cracking. × 1. (B) Ferrite segregation in weld deposit. Etched with 2% Nital. × 100. (C) Details of structure. Etched with 0.5% hot picric acid. × 1000.

Specimens that were first mechanically polished with No. 00 emery were electropolished for 5 to 10 min. Where the mechanical polishing was carried out to a fine finish, only 1 to 2 min of electropolishing were needed.

As shown by some of the accompanying illustrations, a light etch was at times used in conjunction with the electropolishing to reveal the structure surrounding the cracks. Mechanical polishing usually preceded the electropolishing.

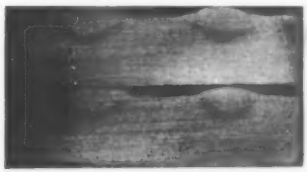
In addition to weld bead cracks we have, in a few instances, encountered underbead defects. The latter do not necessarily show on radiographic or magnetic inspection. Sectioning is often required for their examination. The underbead defects observed were not necessarily hydrogen or cold cracks. It is frequently found that these cracks contain foreign substances originating in the welding rod or parent metal. This is true even when uncoated rods are used and there is no external source from which these materials can be introduced by the process used (inert arc or atomic hydrogen).

Figure 1 shows an atomic hydrogen welded circular bead specimen employing modified SAE 4330 filler rod. Cooling was arrested and postheating conducted at 600° F for 50 min. No cracks appeared on the ground surface of the specimen upon magnetic particle examination. When sectioned, however, what at first appeared to be an underbead crack was found. A more detailed examination showed this indication to consist of inclusions believed to be duplex oxides as shown in Fig. 1*B*. These are shown at a higher magnification in Fig. 1*C* with the associated structure being illustrated in Fig. 1*D*.

NATURE OF COLD CRACKS

The nature of cold cracks has been the subject of considerable discussion. Some

(*A*)



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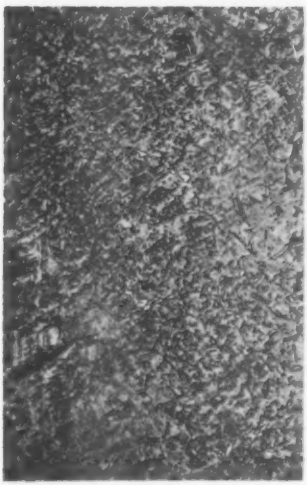
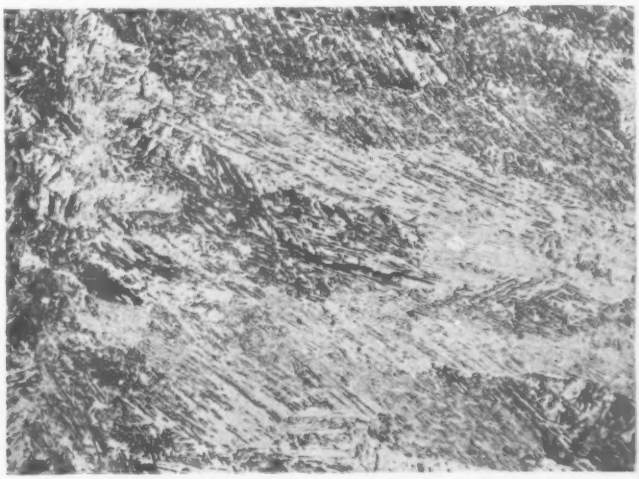


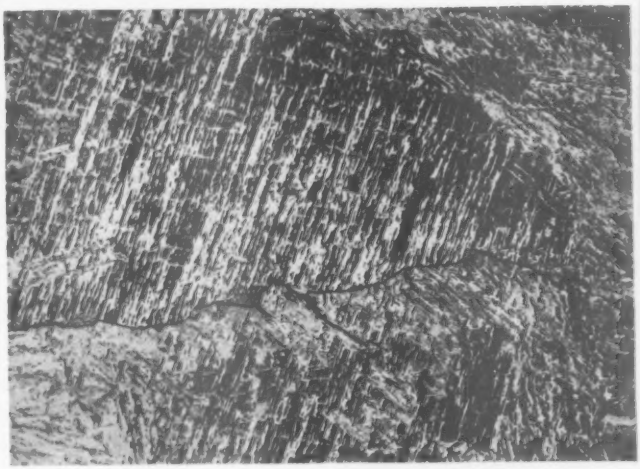
(*B*)

Fig. 5 Atomic-hydrogen welded with modified 6130 filler rod. Air-blast cooled to 1050° F and water quenched. No postheat. Sample A-320

(*A*) Sectioned specimen showing internal cracking. × 1. (*B*) Structure showing tremendous reduction in ferrite segregation over that quenched from 950° F. × 100.



(*B*)



(*C*)

Fig. 6 Atomic-hydrogen welded with modified 6130 filler rod. Air-blast cooled to 1250° F and water quenched. No postheat. Sample A-318

(*A*) Sectioned specimen showing internal cracking. × 1. (*B*) Structure showing very little ferrite segregation and transcrystalline crack. × 100. (*C*) Structure showing very little ferrite segregation and intercrystalline crack. Etched with 2% Nital. × 100.

investigators have stated that such cracks are characteristically transcrystalline. The authors however, have found that both transcrystalline and intercrystalline cracks are common.

A series of tests were made under widely different conditions to study the nature of the resulting cracking. Specimens were welded with the atomic hydrogen process.

Figure 2*A* shows a typical intercrystalline crack in the bead of a circular groove specimen resulting from cooling to 200° F with an air blast and then quenching in water. Figure 2*B* shows another portion of the same specimen in which both intercrystalline and transcrystalline cracking has occurred.

Figure 3*A* shows a similar specimen sectioned to show part of the internal cracking. Figure 3*B* shows the position of a crack which was intergranular in this area. Figure 3*C* shows a part of this same crack at higher magnification. It will be observed that the crack follows the boundary of an area having considerable ferrite precipitation. When the specimen was subjected to an alkaline chromate etch the dendritic structure was revealed with the crack running between and through the dendrites as shown in Fig. 3*D*.

To carry these studies further a series of additional test specimens were air-blast cooled and quenched at successively higher temperatures up to 1250° F with cracking results similar to those just described. In the range of from 950 to 1250° F a significant and anticipated change in the amount of ferrite segregation occurs.

Figure 4 shows a specimen that was air-blast cooled to 950° F before quenching. *A* is a cross section of the specimen showing the internal cracking. *B* shows the microstructure at 100 ×, clearly showing the ferrite segregation. *C* shows the microstructure at 1000 × magification for increased clarity of details. No effort was made to show the cracks because of their similarity to those shown previously in Fig. 3.

Figure 5 shows the specimen which was air-blast cooled to 1050° F before quenching. *A* shows a cross section of the specimen indicating that internal cracks are also prevalent in this instance. *B* shows the microstructure at 100 × revealing that a tremendous reduction in ferrite segregation occurs in this temperature range.

Figure 6 shows the specimen which was air-blast cooled to 1250° F before quenching. This specimen was comparable to one which was cooled to 1150° F and which were therefore not photographed. *A* again shows a cross section of the specimen with its internal cracks. *B* shows the microstructure and it will be observed that very little ferrite is visible. This picture also shows a transcrystalline crack. *C* shows another area of the same specimen containing an intergranular crack. Again very little ferrite segregation is visible although other areas showed a slightly greater amount.

As previously explained, the preceding specimens (Nos. A-321, A-320, A-318) were air-blast cooled to the indicated temperature and then immediately quenched in water.

An additional series of specimens was prepared wherein the pieces were air-blast cooled to specified temperatures and then held for 1 hr at that temperature. This is the arrested cooling and postheating procedure described in Reference 1 which has proved to be very effective in eliminating microcracking when the optimum time and temperatures are employed. The purpose of these tests was to study the microstructure associated with the various arresting and postheating temperatures. All specimens were atomic-hydrogen welded, using modified 4330 filler rod.

Specimen A-213 (Fig. 7) was postheated at 300° F and then quenched in water. Cracking occurred and the microstructure shows primarily ferrite (the white areas) with carbides (black) and possibly some martensite (small gray patches).

Specimen A-218 (Fig. 8) was postheated at 400° F and then quenched (as were all other specimens of this group unless otherwise mentioned). A few faint crack indications can be seen on the original photograph. The microstructure is similar to that of Specimen A-213 excepting that the proportion of ferrite is lower.

Specimen A-224 (Fig. 9) was postheated at the optimum temperature of 600° F and no crack indications were visible on the ground surface with magnetic particle inspection. The microstructure shows complete transformation to carbide and ferrite.

Specimen A-242 (Fig. 10) was postheated at 700° F. A few faint indications can be seen which may be very small or incipient cracks. The microstructure in this case shows a very small amount of martensite (small gray patches) in addition to ferrite and carbide.

Specimen A-377A (Fig. 11) was postheated at 800° F. Cracking has frequently (though not always) been observed in specimens postheated at this temperature. The photomicrograph is of a different but identical test specimen and the structure shows a definite amount of martensite.

It has been observed that specimens air-blast cooled to 200° F from this postheat

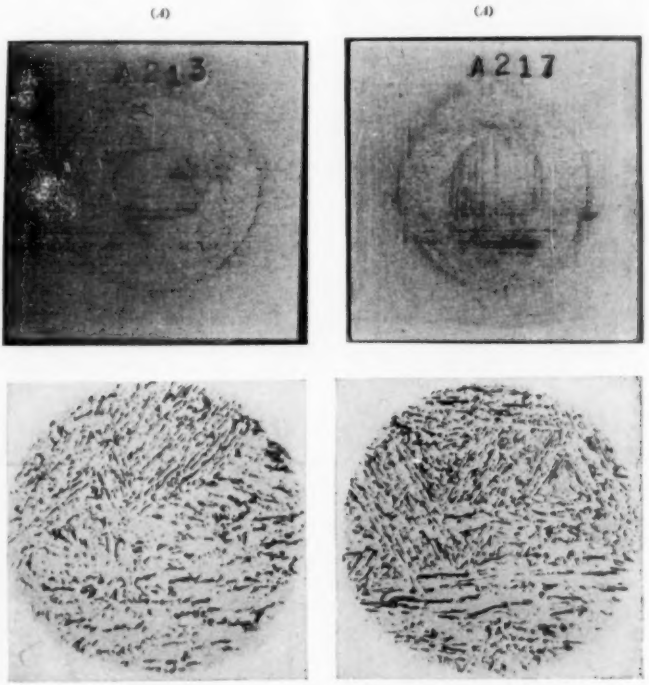


Fig. 7 (left) and Fig. 8 (right) Atomic-hydrogen welded with modified 4330 filler rod. Air-blast cooled to the indicated temperature and held for 1 hr, then quenched in water

(*A*, Figs. 7 and 8) magnified 1 ×. (*B*, Fig. 7) Temperature 300° F, magnified 1000 ×. (*B*, Fig. 8) Temperature 400° F, magnified 1000 ×. Etched with 0.5% hot picric for 15 sec.

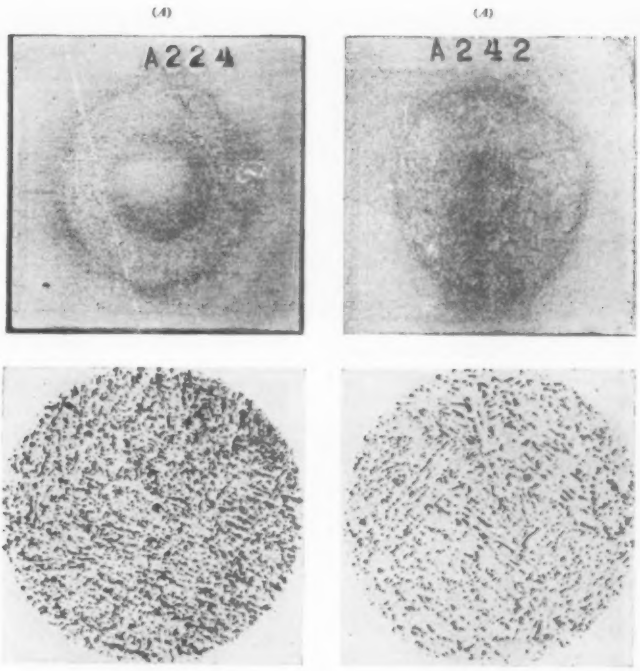


Fig. 9 (left) and Fig. 10 (right) Atomic-hydrogen welded with modified 4330 filler rod. Air-blast cooled to the indicated temperature and held for 1 hr and then quenched in water

(*A*, Figs. 9 and 10) magnified 1 ×. (*B*, Fig. 9) Temperature 600° F, magnified 1000 ×. (*B*, Fig. 10) Temperature 700° F, magnified 1000 ×. Etched with 0.5% hot picric for 15 sec.

temperature before quenching also crack but not quite as severely as when quenched directly from the post heat temperature.

Specimen A-241 (Fig. 12) was postheated at 900° F. Cracking did not occur on this specimen. A considerable amount of martensite is visible in this photomicrograph.

Specimen A-226 (Fig. 13) was postheated at 1000° F. Numerous cracks are visible. The microstructure is now predominantly martensitic with a considerable amount of ferrite and a small amount of undissolved carbides.

It was evident that postheating or even stress relieving for 1 hr at temperatures appreciably higher than the optimum would not prevent cracking if the metal contained sufficient hydrogen. This was verified by the next series of tests using low-hydrogen welding processes which produced crack-free specimens.

Specimen H-237-B (Fig. 14*A*) was welded with the argon-arc process with cooling arrested and postheating conducted at 800° F followed by quenching in the same manner as Specimen A-377A (Fig. 11). Note the freedom from cracking as contrasted with the atomic-hydrogen-welded specimen. A submerged-melt-welded specimen, U-1435A (Fig. 14*B*), was subjected to the same cooling and postheating procedure and is also free of cracks.

POSTHEAT OBSERVATIONS

Atomic-hydrogen welded, circular weld bead, crack sensitivity specimens displayed a tendency to develop microcracks if postheated at 800° F instead of at 600° F. This was true regardless of whether the test pieces were water quenched directly from the postheating temperature or whether they were cooled to 200° F after postheating and before quenching. (Previous tests had indicated that quenching from 200° F gave the same results as air cooling to room temperature.) This condition was observed with both of the filler rods employed, namely, modified SAE 4330 and 6130. Specimens welded with low-hydrogen processes do not crack under similar thermal treatments, indicating that hydrogen is the important factor influencing the cracking.

The cracking of the atomic-hydrogen specimens may be due to the fact that the steel is capable of holding increasing amounts of hydrogen as its temperature is increased and equilibrium is reached at higher hydrogen levels when postheated at 800° F than is the case at 600° F. This high hydrogen level may subsequently cause cracking upon cooling and the formation of martensite.

Another possibility is also predicated upon the high hydrogen equilibrium level resulting from the 800° F postheat temperature. Upon subsequent cooling this hydrogen can no longer escape freely from the metal. As previously explained, however, this hydrogen may diffuse slowly through the rifts described by Smith.[8] Upon encountering molecular-sized discontinuities in the steel, conversion to molecular hydrogen occurs accompanied by an increase of pressure sufficient to cause cracking.

Additional tests are contemplated for the purpose of determining whether the cracking occurs while the specimen is still held at the 800° F temperature. This will be accomplished by postheating the specimen for 1 hr at 800° F, followed by 1 hr at 600° F. The 600° postheat should result in equilibrium at a low hydrogen level and at a temperature at which the hydrogen is still mobile and can escape freely. Transformation also surely should be completed under these conditions. Any observed cracking should, therefore, have occurred at the 800° temperature due to causes yet to be determined.

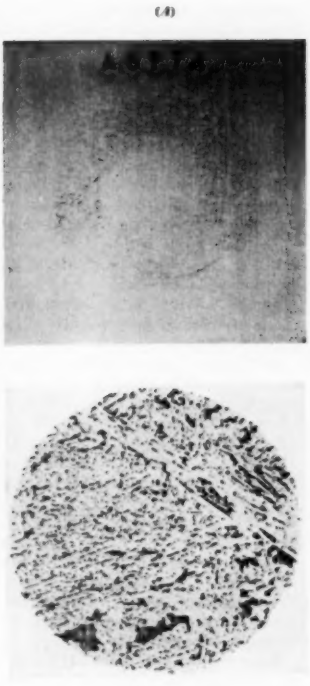
Figure 15 shows a butt-welded specimen which apparently contradicts the crack-free characteristics of hydrogen-free weld deposits. This specimen consists of two bevel-edged plates approximately 20 in. long made of modified SAE 4320 steel, multipass welded by the metal-arc process. The electrodes consisted of modified 4320 core wire with a low-hydrogen lime type coating and were baked at 600° F for 1 hr prior to use. No preheat or postheat was employed on the work.

While no cracks were immediately apparent, the photograph shows two transverse cracks which developed about 3 hr after welding and others which developed within 24 hr.

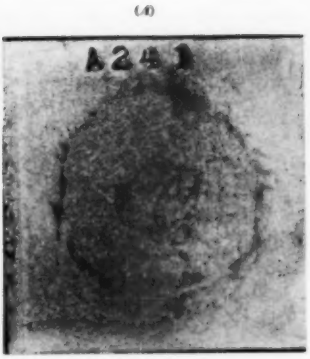
The weldment was free of external restraints. The deposit, however, was subject to high longitudinal restrains due to the longitudinal shrinkage of the molten metal which was resisted by the relatively cool and rigid adjacent parent metal. Shrinkage of the deposit may approximate $^1/_4$ in. to the foot.

The delayed action effect of the cracking is similar to that experienced in weldments known to contain hydrogen and seems to indicate that the gas was present in spite of the use of low-hydrogen electrodes. It must be borne in mind that these electrodes are just what their name implies, "low hydrogen" and not hydrogen free. In multipass welding the hydrogen level may build up to an appreciable degree.

The mechanism of cracking may be either of those previously mentioned: (1) Rejection of hydrogen upon delayed transformation of austenite. (2) Slow



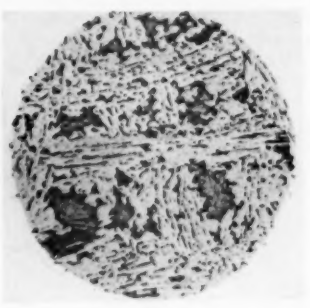
(A)



(A)

(A)

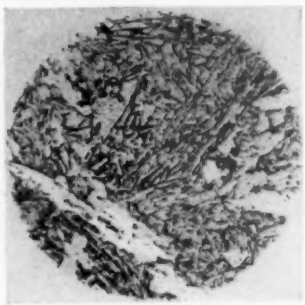
(B)



(B)

Fig. 11 (left) and Fig. 12 (right) Atomic-hydrogen welded with modified 4330 filler rod. Air-blast cooled to the indicated temperature and held for 1 hr and then quenched in water. Note cracks in Specimen A-377A

Fig. 11: (A) Magnified 1 ×. (B) Magnified 1000 ×, temperature 800° F. Fig. 12: (A) Magnified 1 ×. (B) Magnified 1000 ×, temperature 900° F. Etched with 0.5% hot picric for 15 sec.



(B)

Fig. 13 Atomic hydrogen welded with modified 4330 filler rod. Air-blast cooled to the indicated temperature and held for 1 hr and then quenched in water.

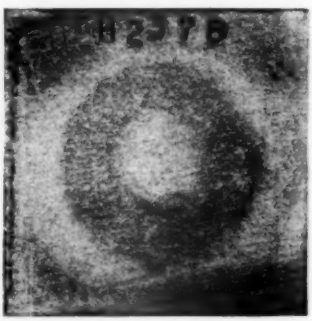
(A) Magnified 1 ×. (B) Magnified 1000 ×, temperature 1000° F. Etched in 0.5% hot picric for 15 sec.

migration of atomic hydrogen until conditions favorable to its conversion to the molecular state are encountered. In other words the delayed-action cracking effect could be due to the slow migration of atomic hydrogen rather than delayed transformation of austenite.

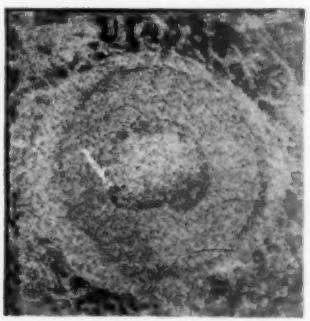
When similar weldments made with the subject electrodes were preheated and cooling was arrested at the 600° F postheat temperature there was no cracking. When the submerged-arc and inert-arc processes were employed without postheat no cracking resulted. These latter processes introduce little if any hydrogen into the weld deposit.

Again we find that if hydrogen is prevented from entering a weld or if it is driven out of the deposit at the proper temperature, stress alone does not appear to cause microcracking.

That stress and hydrogen both play a part in promoting this type of cracking is borne out in Fig. 16. In this case slots were cut into the test plate perpendicular to the joint and prior to welding. These slots overcame the high longitudinal stiffness of the plates and permitted them to yield as the weld deposit shrank. Welding was performed in the same manner as Sample M-176 and no cracking occurred.



(A)



(B)

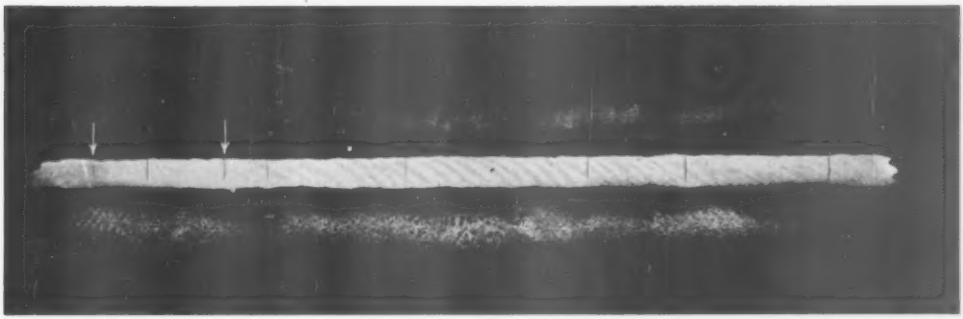
Fig. 14 Circular bead specimens made with low-hydrogen processes. Modified 6130 filler rod. Air-blast cooled to 800° F and held at that temperature for 1 hr and water quenched

(A) Argon-arc weld. × 1. (B) Submerged arc weld. × 1. Note freedom from cracking and compare with Fig. 11A of cracked atomic-hydrogen weld.

It is evident that a considerable amount of occluded hydrogen can be tolerated if the stresses can be kept down. In the absence of hydrogen the stress levels can rise to very high values without causing cracking.

CRACKING DUE TO STEAM

Specimens welded with low-hydrogen



Direction of welding ——➤ Magnification: × 1/4

Fig. 15 Transverse cracks in weldment M-176, metal-arc welded with 4320 low-hydrogen electrodes. The cracks indicated by the arrows appeared approximately 3 hr after welding. The other cracks appeared 6–21 hr after welding.

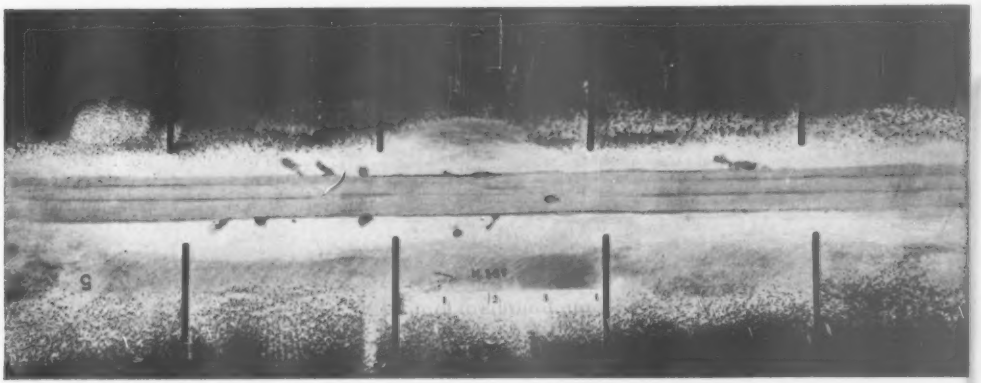
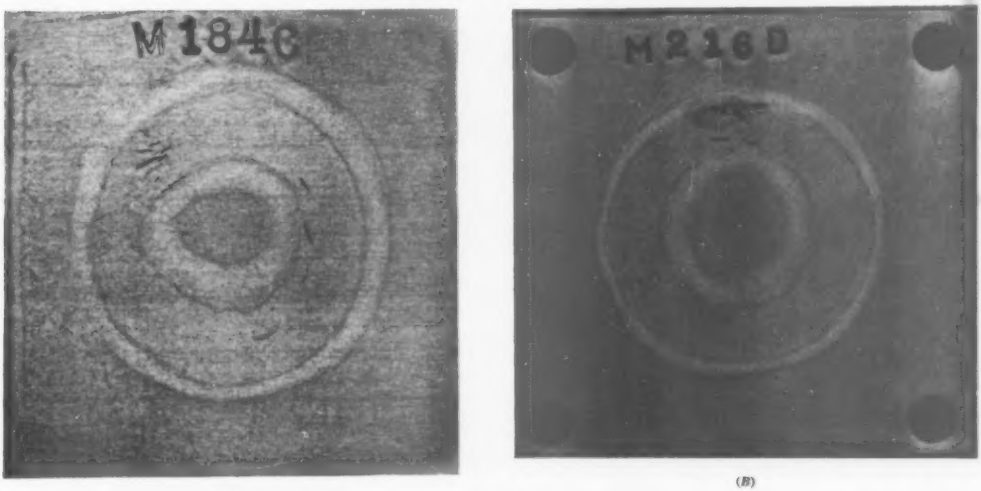


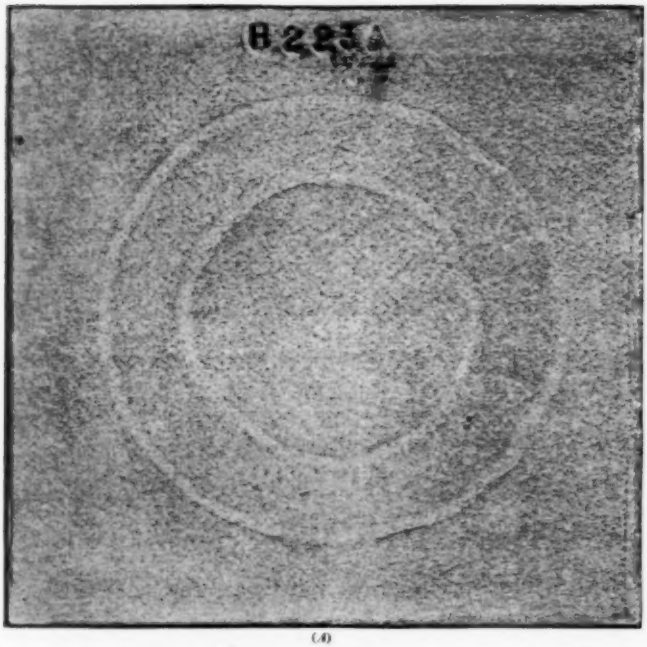
Fig. 16 Specimen M-189 metal-arc welded with 4320 low-hydrogen electrodes. Plates were slotted prior to welding for the purpose of minimizing the longitudinal restraint. Note absence of cracking. × 3/8



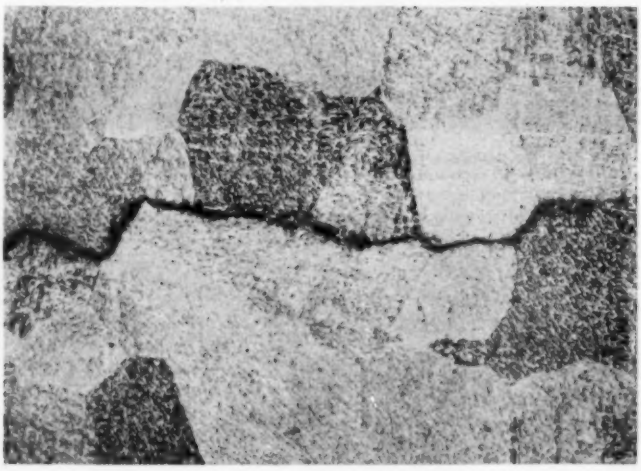
(*A*) (*B*)

Fig. 17 Metal-arc welded with low-hydrogen electrodes having 4320 core wire. Cooled during welding by jet of water against the underside of the specimen

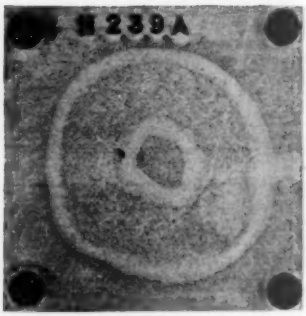
(*A*) Weld zone not isolated from steam. × 1½. (*B*) Weld zone was isolated from steam. × 1½.



(A)



(B)



(C)

(A) Magnified 1 ×. (B) Magnified 100 ×. (A) and (B) not isolated from steam. (C) Magnified 1 ×. Isolated from steam. Note complete freedom from cracks.

Fig. 18 Argon-arc welded specimen welded with 4330 filler rod. Cooled during welding by a jet of water against the underside of the specimen

processes were made using various water-cooling procedures to obtain very high cooling rates. Certain of these cooling methods resulted in considerable quantities of steam or water entering the welding zone. This stream nullified the low-hydrogen properties of the process and induced cracking in the weld bead. The cracking appears to be due to hydrogen occlusion through the steam-metal reactions described by Zapffe.[4] Where effective measures were taken to prevent steam from entering the welding zone, no cracking was observed even with the drastic quenching resulting from welding while the specimen was partially submerged and subjected to a jet of water against the back. These investigations appear to confirm that as the hydrogen level of the weld deposit is decreased, the cooling rate becomes of less importance.

No atomic-hydrogen welded specimens are included in this high-cooling rate series of tests because even with air cooling such weld deposits contained numerous microcracks. Low-hydrogen welding processes were therefore selected for this work.

Figure 17*A* shows a specimen welded with the low-hydrogen metal-arc electrodes previously described. The lower 1/4 in. of the specimen was immersed in water and a jet of water impinged against the underside. There was no provision for keeping steam and water vapor from the weld zone. The cracks that resulted are clearly visible.

In Figure 17*B* is shown a similar specimen with the exception that the cooling fixture was modified to prevent steam and water vapor from reaching the weld zone. There were only one or two cracks visible by magnetic particle inspection. With electropolishing, approximately 10 to 20 cracks became visible on the several test specimens. It is perhaps significant that in this case all of the cracking was restricted to the weld crater, the balance of the deposit being crack-free. Some flux entrapment occurred due to the high cooling rate. The form of crater cracking which was observed was probably due to the loss of protective atmosphere which occurs with shielded electrodes upon extinguishing the arc. It would not have been surprising to have encountered more extensive cracking in these metal-arc-welded specimens. The coatings of low-hydrogen electrodes are not completely hydrogen free and can contain sufficient hydrogen to cause cracking under high cooling rates.

Figure 18*A* shows an inert-arc-welded specimen produced under the same conditions of drastic cooling previously described. Even when welded without isolating the weld zone from the evolved steam, and with the more highly crack-sensitive 4330 filler rod there was very little

cracking. There were probably at least two factors responsible for these results. First, the flow of argon gas undoubtedly kept the major portion of the steam away from the molten metal. Second, this specimen was much larger than those used in previous tests and somewhat less steam resulted. The small amount of steam which did succeed in diluting the protective atmosphere, however, caused a few small cracks in the deposited metal. The intergranular nature of these cracks is shown in the photomicrograph Fig. 18*B*. When similar specimens were welded with the same high cooling rate but with the weld zone isolated from the steam, no cracking resulted as evidenced by Fig. 18*C*.

Specimens which were submerged arc welded under similar cooling conditions, cracked when steam was permitted to enter the weld zone or when the flux became moist as shown in Fig. 19*A*. When the weld zone was isolated from the steam no cracking occurred as illustrated in Fig. 19*B*. In some instances "worm-hole" porosity developed in submerged-arc-welded specimens subjected to steam. Figures 20*A* and 20*B* show the top and cross-sectional appearance, respectively, of a sample showing this form of defect.

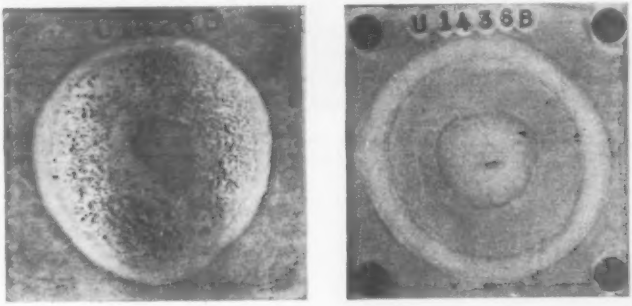
Since both the inert-arc and submerged-arc processes are essentially hydrogen free, these investigations appear to give additional evidence that as the amount of occluded hydrogen in the weld deposit is decreased, the cooling rate becomes of less importance. Where there is essentially no occluded hydrogen (as with the submerged-arc and argon-arc processes) the most drastic cooling rates thus far employed by the writers failed to induce cracking.

The foregoing leads one to conjecture that perhaps the results reported by Rollason and Roberts[8] may have been affected by the steam produced by the hot metal of the specimen in contact with the quenching water. In their paper it was reported that even deposits made with low-hydrogen electrodes developed cracks when welded while partially submerged and with a jet of water impinging against the underside of the specimen. They attributed this cracking solely to the high cooling rate which resulted. As previously indicated, however, it is probable that the low-hydrogen properties of the electrodes employed were largely nullified by the enormous hydrogen potentials developed through the steam-metal reactions described by Zapffe.[4] The reported temperatures of the specimens were of such order that considerable quantities of steam were obviously produced. It is also quite likely that the electrode coatings employed introduced sufficient hydrogen into the deposits to produce some cracking under the conditions of test. There is, however, no evidence indicating that the cooling rate alone was responsible.

THE EFFECT OF STRESS DIRECTION UPON MICROCRACKING

A series of tests were conducted for the purpose of determining the influence of the magnitude and direction of stress upon cold cracking. V-butt welded specimens were employed consisting of two plates $^1/_2$ in. thick by $3^1/_2$ in. wide by 20 in. long of the same modified 4320 steel used for the other specimens described in this paper. The samples were metal arc welded with the low-hydrogen coated electrodes· 4320 core wire was used. All test pieces were welded while the lower $^1/_8$ in. was submerged in a pan of water.

Figure 21 shows a specimen welded without restraint other than that due to the rigid adjacent parent metal which resisted the longitudinal shrinkage of the deposit. As may be observed there are numerous transverse cracks in the second



(*A*) (*B*)

Fig. 19 Submerged arc welded with 4330 filler rod. Cooled during welding by a jet of water against the underside of the specimen

(*A*) Weld zone not isolated from steam. × 1. (*B*) Weld zone was isolated from steam. × 1

(*A*)

(*B*)

Fig. 20 Submerged arc welded with 4330 filler rod. Cooled during welding by jet of water against the underside of the specimen

Weld zone was not isolated from steam. Note worm-hole type of porosity. Magnification: (*A*) × 3. (*B*) × $2^1/_2$.

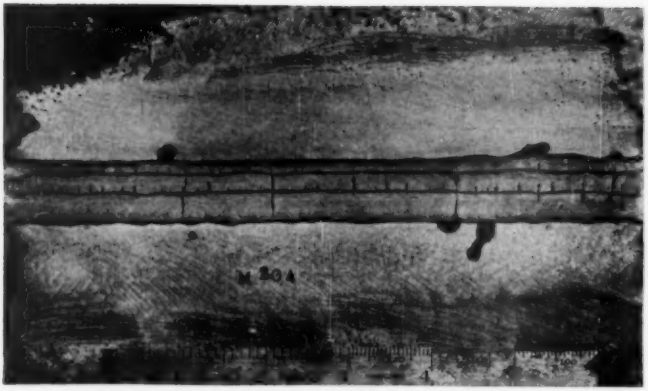


Fig. 21 Metal-arc welded with low-hydrogen 4320 electrodes water cooled during welding. No external restraint

and third from the last beads, several of which propagated through all of the last three beads. None of the cracks seemed to originate in the final bead. The cracks mentioned may be due to the tensile stresses caused by the expansion of the solidified deposit from the heat of the succeeding bead, together with the hydrogen resulting from the steam entering the weld zone. Magnetic particle examination of the parent metal failed to reveal any cracks.

Similar specimens were welded with high transverse restraint in addition to the longitudinal restraint offered by the adjacent parent metal. The transverse restraint was achieved by welding the long outside edges of the plates to a heavy ($1^1/_2$ in.) bed plate which formed the bottom of the cooling water pan. As illustrated in Fig. 22*A*, the transverse cracks due to the longitudinal restraint were again observed. With this biaxial restraint, however, there were also several longitudinal cracks in the next to the last bead due to the transverse restraint. It was again noticed that no cracks appeared to originate in the last bead although several cracks in preceding beads propagated into this bead. In addition to the cracks appearing upon the upper surface, this weldment also contained a very large longitudinal crack and a few transverse cracks on the underside where full penetration had been achieved as shown in Fig. 22*B*.

These tests made it evident that direction of stress had a decided influence upon the direction of cracking. The continued predominance of the transverse cracking appeared to indicate that the greater amount of shrinkage which occurred along the longitudinal dimension was a controlling factor. To explore this assumption further, another test specimen was prepared in such a manner as to greatly reduce the longitudinal restraint. This restraint being due to the relatively cold and rigid parent metal adjacent to the weld was minimized by cutting a series of slots as shown in Fig. 23*A*. These slots introduced a degree of longitudinal freedom to the specimen. In all other respects this weldment was similar to that in Fig. 22, including the transverse restraint and the cooling.

A marked change in the nature of cracking was immediately evident. The longitudinal cracking was now very prominent, although none was observed on the underside of the weldment as may be seen in Fig. 23*B*. The transverse cracking was greatly reduced in magnitude or size though considerably greater in number. It was interesting to observe that in the slotted sample, as in the others, there was less tendency for transverse cracking to originate in the last pass. There seemed to be little difference in the longitudinal cracking tendencies of the various passes.

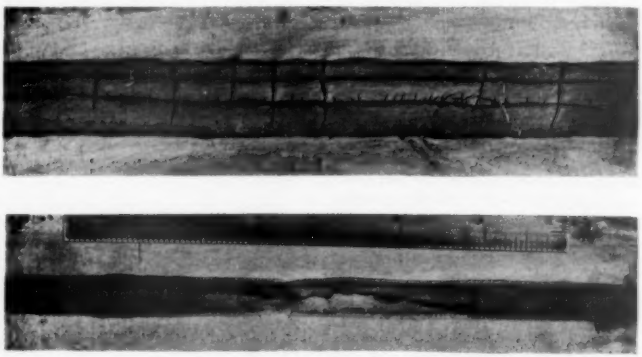
As was expected, these tests confirmed that cracking normally occurs perpendicular to the direction of the applied stress. They also showed that where conditions favor the introduction of hydrogen, the degree of cracking will be influenced by the magnitude of the applied stress.

While welding all of these specimens considerable quantities of steam were evolved from the cooling water. This steam undoubtedly reacted in the welding zone to introduce sufficient hydrogen to materially increase the degree of cracking.[4] For the purpose of the tests this was a desirable condition as we wanted to insure that cracking would occur.

It is proposed to extend this phase of the investigation in the future to include measurements of the actual stresses involved through the use of strain gages.

MICROCRACKING IN MILD STEELS

While it was not previously believed that mild steels were subject to hydrogen cracking or embrittlement, in recent years various investigators have shown that such steels are subject to hydrogen damage. Bland,[7] Rollason and Roberts,[8] Flanigan and Kaufman[10] and others have made valuable contributions toward a more thorough understanding of this subject. It has been shown that in many cases the microcracks in mild steel are much smaller than those which occur in alloy steels but nevertheless such cracks may propagate under some conditions of service, possibly resulting in ultimate failures. Considering the small size of some of the microcracks found in mild steels, more detailed methods of examination are often required for their investigation. Flanigan and Kaufman have shown that mechanical polishing prior to etching for microexamination will often drag metal over the crack so that it cannot be seen. Electropolishing, however, opens up the cracks, making them subject to

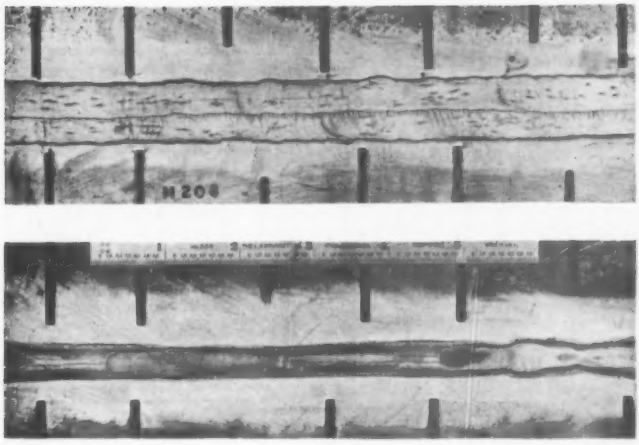


(A)

(B)

Fig. 22 Metal-arc welded with low-hydrogen 4320 electrodes. Water cooled during welding. Transverse restraint and inherent longitudinal restraint. (A) Top surface. (B) Bottom surface

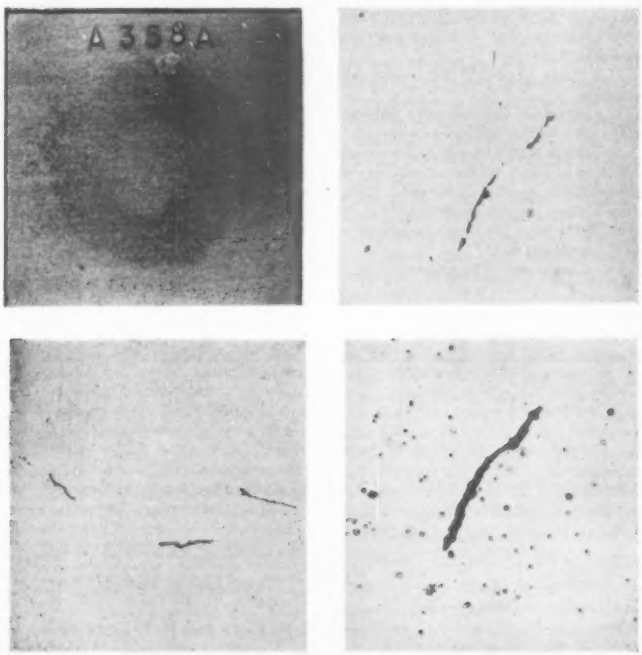
(A)



(B)

Fig. 23 Metal-arc welded with low-hydrogen 4320 electrodes. Water cooled during welding. Transverse restraint with inherent longitudinal restraint relieved by slotting. Only a part of the full specimen width is shown. (A) Top surface. (B) Bottom surface

(A) (C)



(B) (D)

Fig. 24 Atomic-hydrogen welded with AISI-1012 filler rod on SAE 1020 plate. Air-blast cooled to 200° F and water quenched. Stress relieved at 1060° F for 1 hr

(A) Magnetic particle inspection reveals no cracks. × 1. (B) Microcracks revealed after mechanical polishing. × 300. (C) Microcrack revealed after mechanical polishing. × 300. (D) Microcrack shown in (C) after electropolishing. × 300.

detection under the microscope either with or without subsequent etching.[10]

The writers undertook studies of a few mild steel deposits. In the absence of information regarding the electropolishing bath employed by Flanigan and Kaufman, the Weisman solution previously described was employed.

Figures 24 and 25 show two specimens, Nos. A-358A and A-358B, respectively, which were atomic-hydrogen welded. The parent metal was SAE 1020 plate and the filler material was a popular mild steel rod approximating an AISI 1012 analysis. The specimens were not postheated but were air-blast cooled to 200° F and then cooled in water and stress relieved. They were then ground to 0.100 in. below the original surface and magnetic-particle inspected in the usual manner. No cracks

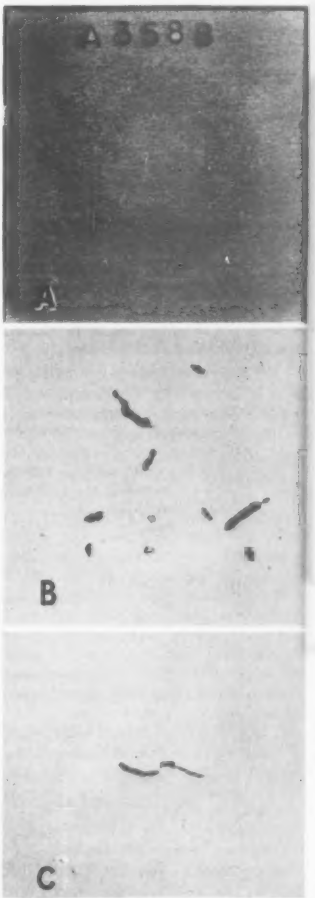


Fig. 25 Same welding conditions as Sample A-358A shown on Fig. 24

(A) Magnetic particle inspection reveals no cracks. × 1. (B) Group of microcracks revealed after electropolishing. × 70. (C) Microcracks revealed at 400 × after electropolishing. × 300.

could be detected with this examination. With normal microexamination a few cracks could be found, some of which were very difficult to detect. When the specimens were electropolished, however, numerous cracks were observed.

Figure 24*B* shows a group of small cracks which were detected after mechanical polishing. Figure 24*C* shows a larger crack also detected after mechanical polishing. When this specimen was electropolished for 1 min the crack became much more pronounced.

Figure 25*B* shows a group of microcracks in Specimen A-358B which could be detected only after electropolishing whereupon they were readily observed at only 100 × magnification. Figure 25*C* shows two much smaller cracks which also were detected only after electropolishing. In this case higher magnification was necessary to show them clearly.

The greater effectiveness of electropolishing for the study of this type of fissure is quite evident.

SUMMARY AND CONCLUSIONS

1. This paper presents an extension of earlier work on the factors influencing microcracking of certain types of steels.

2. Portions of several theories of hydrogen cracking are reviewed. The possibility that hydrogen is not contained in steel interstitially is discussed with reference to evidence indicating that hydrogen may be contained in rifts or occasional widening of the lattice spacings. Hydrogen cracking may be due to:

(*a*) Rejection of the gas from martensite as it forms, thus generating enormous aerostatic pressures.

(*b*) Migration of hydrogen to rifts or discontinuities of molecular size or larger which promote conversion of the gas to the molecular state with consequent increase in aerostatic pressure to destructive magnitudes.

3. Delayed cracking of weldments may be caused by:

(*a*) Delayed transformation of austenite with subsequent rejection of hydrogen.

(*b*) Slow migration of atomic hydrogen to zones in which conversion to the molecular state can occur.

4. Both transcrystalline and intercrystalline hydrogen cracks were observed.

5. Arrested cooling and postheating at 600° F resulted in crack-free welds regardless of stress or the amount of hydrogen initially introduced.

6. Specimens postheated at 800° F and above (with the exception of those postheated at 900° F) often but not always developed cracks.

7. For reasons unknown, none of the specimens postheated at 900° F developed cracks.

8. Weld deposits essentially free of occluded hydrogen did not crack even at the very high cooling rates resulting from welding while quenching with water.

9. When even small amounts of hydrogen entered the weld zone, high cooling rates caused cracking.

10. Steam or water vapor entering the weld zone nullified the low hydrogen characteristics of the various processes which would normally not contain detrimental amounts of the gas.

11. Stress alone did not cause cold cracking.

12. Hydrogen-free deposits did not crack even under high stress or restraint.

13. Weld deposits become increasingly sensitive to stress as the amount of occluded hydrogen was increased. Stress may increase the rift dimensions to a degree that permits the hydrogen to change to the molecular form.

14. Cracks caused by a combination of occluded hydrogen and stress were oriented in a direction perpendicular to the direction of stress.

15. Mild steel weld deposits containing hydrogen are also subject to hydrogen cracking if the cooling rate is sufficiently high.

16. Cracks in mild steel weld deposits are usually much smaller than those occurring in hardenable steels.

ACKNOWLEDGMENT

The authors are deeply grateful for the encouragement and support of W. C. Schulte, Chief Metallurgist. Welding of the test pieces was performed by Dean Henry and Jack Smith while specimens were prepared and processed by J. J. Scannelli. Photography and other phases of the program were accomplished through the coordinated teamwork of numerous members of our Metallurgical staff.

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Welded Continuous Frames and Their Components
Progress Report No. 4

Connections for Welded Continuous Portal Frames

Part III—Discussion of Test Results and Conclusions

Elastic and plastic strength of rigid frame connections, economies, connection design details and structural behavior

by Lynn S. Beedle, A. A. Topractsoglou and Bruce G. Johnston

1. DISCUSSION OF TEST RESULTS

THE objectives of the investigation, outlined in Part I, form the basis for the arrangement of this discussion of test results. First, the results are examined to see if the connections satisfy the requirements of elastic design. Are the assumptions of present design practice consistent with safety? Secondly, it is of interest to observe the behavior beyond the elastic limit and up to collapse in order to evaluate the possibilities and limitations of new concepts of plastic design and analysis. These considerations exclude the possibility of failure due to fatigue.

*1. Elastic Strain Distribution in Straight Knees**

Connection P: The portions of Connection *P* outside the knee behaved according to the ordinary beam theory, Fig. 32† and 33.† At sections close to the knee, within 2 in. of the vertical stiffener, the stress distributions become irregular due to end disturbances, this being usual in the case of any end connection or bearing support. However at sections of the beam and column removed from the connection a distance of half the depth of the section, the stresses were in reasonably good agreement with the computed values. As was pointed out in Part II the tensile stresses in the exterior beam flange are transmitted to the knee by shear (Fig. 62†). Such shear stresses must be considered since they may lead to excessive relative rotation between ends of connected members. In Connection *P*, for example, the maximum web shear stress was larger than that at the critical section for bending. Later equations give the required web thickness to prevent these undesirable deformations due to shear force.

Figures 34 and 35 show that there are local strains that differ from the values predicted but the trend is to confirm the assumption for Type 7 connections that bending moment decreases linearly from a maximum at the critical section where the knee joins the rolled section to zero at the exterior corner. In the case of the beam, as shown in Fig. 34, no correction was made for bending in the outer flange since SR-4 gages were mounted on but one side. From Fig. 21, noting the shape of the deformed top flange in the region of the knee, it is seen that the measured strains are consistent with the deflected shape. As expected, the stress is a maximum at the face of the column.

The theoretical distributions of stress shown in Figs. 32-35 take into account both bending moment and direct stress, although the influence of the latter is quite small. Since the lengths of members are short, additional moment due to column deflection is negligible and has not been considered.

2. Elastic Strength of Connections

In this section will be discussed the behavior of the connections at loads in the vicinity of the yield point. The evidence and influence of residual stresses and the formation of "yield lines" as revealed by mill scale will also be discussed.

2A. Shear Yield of Straight Knees Without Diagonal Stiffeners

Equations have been developed in Section I, Part II, to predict the moment at which yielding of Type 7 connections due to shear force should commence and a comparison has been made with theoretical yield moments due to flexure. Connection *P* provided an opportunity to compare theory with experiment, and Fig. 19 of Part I contains, both theoretical and experimental curves. As is evident from this figure, nonlinear behavior of the connection commenced at a moment somewhat lower than the predicted moment at which yielding due to shear force should commence. Subsequent rotation was developed well beyond that which could be tolerated in most engineering structures. The first yield line was observed at 311 in.-kips, nonlinear behavior was observed visually from the plotted curve at 493 in.-kips, and the maximum moment carried was 1150 in.-kips. The theoretical "shear yield" values are 724 in-kips assuming a uniform distribution of shear stress in the knee and 630 in.-kips assuming a nonuniform shear distribution (see Part II of the paper). The predicted initial yield moment in flexure is 1195 in.-kips.

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* This treatment of elastic stress distribution is only partially complete, and it is expected that further data will be presented in a separate report.

† Figures 1 to 57, inclusive, will be found in Part I as published in the July 1951 WELDING JOURNAL, pp. 359-s to 384-s; Figs. 58 to 73, inclusive, are in Part II, August 1951 WELDING JOURNAL, pp. 397-s to 405-s. Figure 52 is included in this report as well as in Part I.

Comparing theory with experiment the first yield line was observed at about 50% of the predicted "shear yield" load, nonlinear behavior was observed about 20% below the predicted value,* and the rate of increase of deflection increased markedly at a load as low as 50% of the calculated flexural yield load. The latter relationship is the one requiring attention

* These reductions are due to stress concentrations and residual stresses and are consistent with observations made in continuous beam tests.[5]

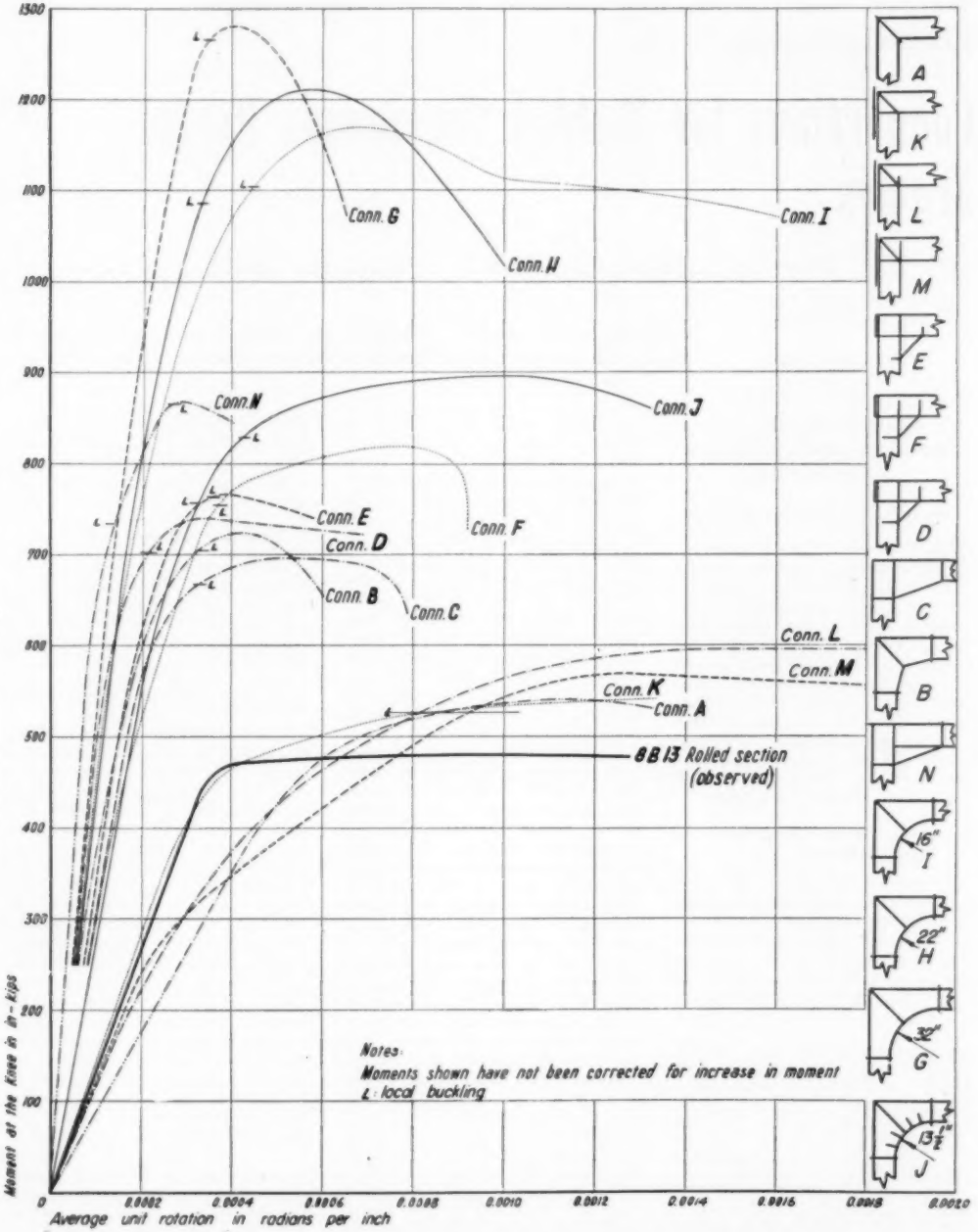


Fig. 52 Connection behavior compared with 8B13 rolled section (Based on moment at the knee)

since the computation of moments at which shear yielding commences is not a part of routine analysis procedures. In spite of the large rotations, the connection did not develop the predicted flexural yield strength of the weakest adjacent member.

The assumption of uniform shear distribution in the knee web gives an approximate prediction of actual behavior, but the assumption of nonuniform distribution of shear stress provides a much better indication of the load at which inelastic deformation of connections with unstiffened webs will commence.

Following the usual steps for proportioning a Type 7 connection the designer would check the shear in the beam and column; but it has been demonstrated here that it is *most* important for him to check the shear in the knee web.† To make sure that the "shear" type of failure does not occur, the moment at which shear yield occurs, $M_{h(\tau)}$, must be equal to or greater than the moment which results in flexural yield, $M_{h(\sigma)}$. These expressions, developed in Part II, are,

$$M_{h(\tau)} = \frac{\sigma_y}{2}\,\frac{wd^2}{(1 - d/L)} \qquad (3)$$

$$M_{h(\sigma)} = \sigma_y \Big/ \left[\left(\frac{1 - \frac{d}{2L}}{S}\right) + \frac{1}{AL}\right] \qquad (4)$$

By equating these two expressions the required web thickness is obtained,

$$w \geq \frac{S}{d^2}\left[\frac{2}{S/AL\left(1 - \frac{d}{2L}\right) + 1}\right] \times \left[\frac{1 - \frac{d}{L}}{1 - \frac{d}{2L}}\right] \qquad (36)$$

L is the distance from the center of the knee (the haunch point) to the load points on the connection arms (Fig. 74). In an actual frame L is the distance between the point of inflection and the haunch point.

Equation 36 may be simplified to a convenient expression for required web thickness. If the proportions of Connection P are assumed ($L/d = 6$), the third factor in eq 36 is 0.91. For 14WF30, 8B13, 21WF82, 6B12, 24WF110 and 8WF31 shapes the second term in eq 36 ranges from 1.870 to 1.888; the average product of the two terms in brackets is 1.71. Of course, the selection of the single L/d ratio is an arbitrary one. Instead of $L/d = 6$, if the basis for comparison has been selected as the "L_u" value for each member (L corresponding to $Ld/bt = 600$), then the product of the last two terms in eq 36 is 1.82. Taking an average value, then, of 1.76, a suggested expression for web thickness w is obtained,

$$w = 1.76\,S/d^2 \qquad (37)$$

This result is based on an assumption of uniform distribution of shearing stress in the knee web. A more conservative relationship than that given by eq 37 would be obtained if the required web thickness to prevent premature inelastic shear deformation were increased by about 15% to take into account the actual nonuniform shear distribution. In this case,

$$w = 2\,\frac{S}{d^2} \qquad (38)$$

By the rule of eq 38 none of the WF shapes would provide sufficient web thickness to give an adequate Type 7 connection without additional stiffening. One WF shape (12WF16.5) is very nearly adequate. One shape of every nominal depth

† It is assumed that the proportions are such that web buckling does not occur in the elastic range of stress.

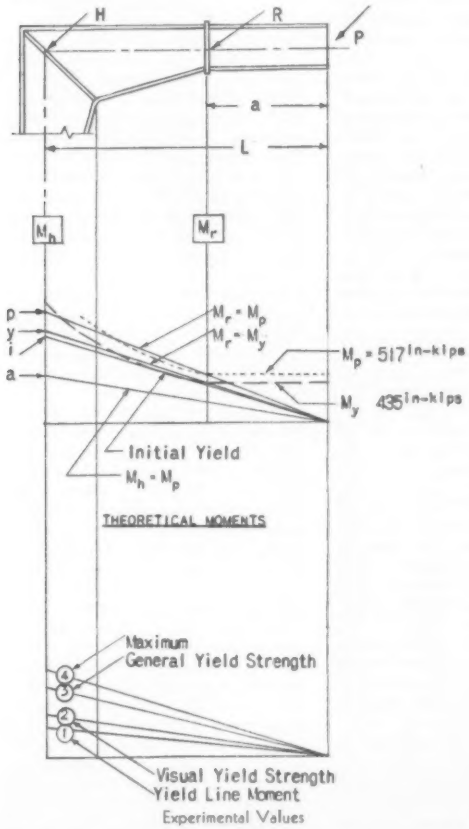


Fig. 74 Explanation of the various designations used in the comparison of connection strength

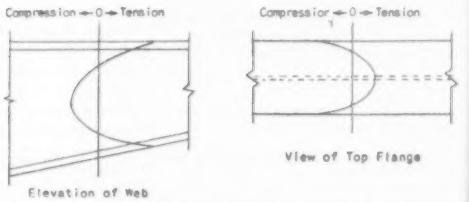


Fig. 75 Hypothetical distribution of residual stresses due to welding

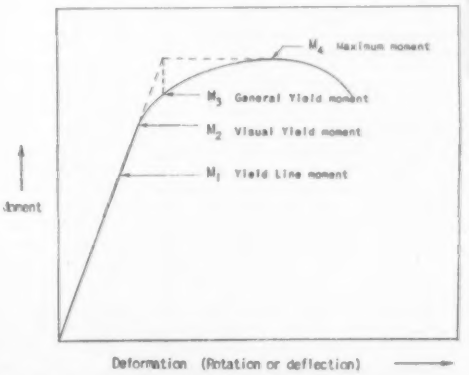


Fig. 76 Four criteria used in this paper for comparing the strength of structural members

has been checked by computation and several shapes within one series have also been studied. The *lighter* members in each nominal size more nearly meet the web thickness requirement and would require the least amount of additional stiffening.

Computations have also been made for American Standard I shapes. For each nominal depth the *heaviest* section theoretically has adequate web thickness without requiring additional stiffening material. Most of the lighter sections in each series have insufficient web thickness.

Additional tests should be conducted specifically for the purpose of checking the validity of eq 38 as a rule for specifying the required thickness of doubler plates or diagonal stiffeners. However, since most rolled shapes are deficient in web thickness for Type 7 connections, it is recommended that design rules require diagonal stiffeners or doubler plates. The 8B connection uses the former detail. If the diagonal plate is objectionable, extra web thickness may be obtained with doublers using eq 38 as a guide. Connections of this type were not tested in the program, although such studies are planned.

In the case of Type 8B connections, eq 38 and the assumptions of Part II may be used to specify the required thickness of diagonal stiffeners. In eq 29 the "effective" area of the web was assumed to be made up of two parts: the actual web area, $A_w = wd$, and an equivalent stiffener area, A_s', arrived at by assuming the stiffener material uniformly distributed over the web plate. From eq 29,

$$A_s' = b_s t_s \sqrt{2}$$

where

t_s = stiffener thickness.
b_s = total width of stiffener.

Since this equivalent stiffener area makes up the deficiency in web thickness, then

$$A_s' = (w_r - w_a)d$$

where w_r is the required web thickness according to eq 38 and w_a is the actual web thickness of the rolled shape and d is the section depth. Equating the two expressions for A_s', the required thickness of diagonal stiffener is given by

$$t_s = \frac{(w_r - w_a)d}{b_s\sqrt{2}} \qquad (39)$$

An examination of Fig. 20 shows that the experimental deflection curve deviates from a straight line at approximately the same moment as that of the rotation curve (Fig. 19). This indicates that knee deformations cause the nonlinear behavior at the low loads. This is clear from Fig. 21 as well. Figure 19 indicates that there is no factor of safety against yielding at a moment corresponding to a working stress of 20 ksi. Thus, Connection *P* is "inadequate" from the point of view of elastic design. As is evident from Figs. 22 and 52 sufficient diagonal stiffening was provided in Connections *A*, *K*, *L* and *M* to prevent serious shear deformation. The photographs of the connections at failure (Figs. 26, 29) show that in spite of the use of diagonal stiffeners, yield due to shear force still occurred at high loads, but with a satisfactory margin of safety for elastic design.

2B. *"Yield Lines" and Residual Stress*

Coating the connections with whitewash revealed the flaking of mill scale at yield zones. "Yield lines" were observed at loads between 31 and 81% of the calculated initial yield load as indicated on the experimental curves (Figs. 22, 36, 37, 44). This yielding at less-than-calculated values is usually due to a combination of residual stresses and stress concentrations. For the larger built-up connections (*B*, *N*, *G*, *H*) the resultant increase in measured rotations or deflections due to formation of these first yield lines is slight indeed (Fig. 44). However, in the remaining connections where the connection length is shorter, then formation of the first yield line may be associated with the commencement of nonlinearity of the load-deformation curve (Fig. 22). The consequences of nonlinear behavior at loads lower than the predicted yield point are usually not serious in the case of members designed to resist flexural loads. However, when one considers column action, wherein yielding with a corresponding reduction in effective bending stiffness aggravates buckling, the possible seriousness of residual stress becomes important.

All of the connections, a number of which were built up by welding (i.e., *B*, *C*, *G*, *H*, *I*), were tested in the as-delivered, as-welded condition. The observed yield line patterns indicate that welding introduced residual stress patterns somewhat similar to those formed due to cooling after rolling. Figure 75 indicates schematically the possible distribution of residual stresses in web and flange material at a cross section through a haunch fabricated by welding. In the tests, yield lines were observed at the edges of the compression flange and at the center of the tension flange.

2C. *Yield Strength of Connection Types*

As in the case of most structural members in bending, the transition from elastic to plastic behavior was very gradual in these connection tests; a well-defined yield point was not observed (Fig. 52). Since functions such as "initial yield load" and "yield strength" of the various connections are now to be compared, then definite criteria will be adopted. Prior to describing the criteria available, the terminology will be defined:

(*a*) *Moment-position of critical section along the member:*

M_h = "haunch" moment—the moment at intersection of extended neutral lines of girder and column (Fig. 74).

M_r = "rolled section" moment—connection moment at junction of rolled beam and knee.

M = moment at any position.

(*b*) *Theoretical or computed moments* (subscripts h and r have been omitted):

M_i = "theoretical initial yield moment" of the connection for a particular loading condition (Fig. 74).

M_y = "theoretical yield moment of the rolled section"—the moment at which yield-point stress is reached at the end of the rolled section.

(*c*) *Experimentally observed moments* (subscripts h and r omitted):

$M_{(1)}$ = "yield line" moment—the moment at which the first yield line is observed (Fig. 74).

$M_{(2)}$ = "visual yield" moment—the moment at which the plotted curve becomes nonlinear as observed visually.

$M_{(3)}$ = "general yield" moment (this criterion is defined below).

Some of the available yield strength criteria are summarized in the following paragraphs. Not all of them have been used in this paper.

(*a*) *Yield Line Moment:* The moment at first yielding, $M_{(1)}$, described above, is a value recorded during the test, and is determined by careful examination of the test member after each load increment (Fig. 76).

(*b*) *Visual Yield Moment:* This has also been described above and is designated in Fig. 76 as $M_{(2)}$. This is an approximate method dependent upon the scale to which the curve is plotted and the judgment of the observer.

(*c*) *General Yield Moment:* $M_{(3)}$ is determined by the graphical method shown in Fig. 76. Originally suggested by one of the authors,[33] it has been termed the "limit of structural usefulness." It may be said to correspond approximately to a point at which inelastic connection deformations would begin to affect structural behavior elsewhere in a continuous frame. Obviously it is an arbitrary deformation limitation.

(*d*) *Scatter Band:* After drawing the experimental curve to a large enough scale to indicate the scatter, the yield strength of the structure or member is defined as the intersection with the experimental curve of a line parallel to the elastic part and offset by one-half the scatter band width. This method has been used by others, but has not been employed in this paper. It is one of the most sensitive criteria.

(*e*) *Slope Factor:* Another method not original with the authors involves the

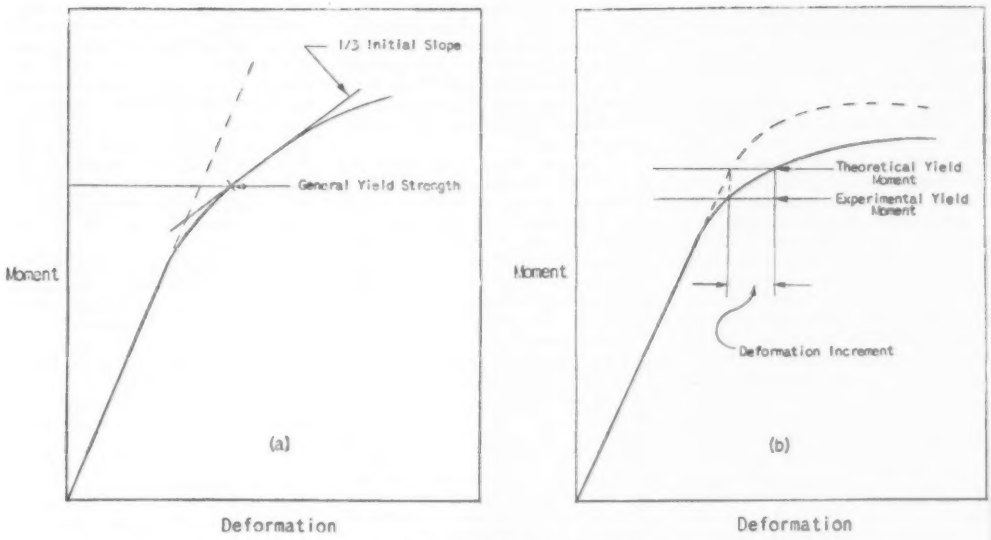


Fig. 77 Additional yield strength criteria

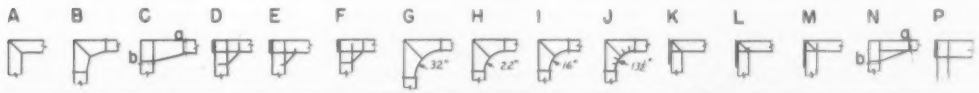
drawing of a line tangent to the test curve at a slope one-third the original elastic slope. The point of tangency is a measure of the yield strength. Comparisons using this method have not been made, but the method is indicated in Fig. 77 (*a*).

(*f*) *Deformation Increment:* Progress Report 5[34] compares the behavior of continuous beams on the basis of the per cent increment of centerline deflection beyond the predicted deflection at the elastic limit moment $M_{h(1)}$. The scheme is shown in Fig. 77 (*b*); it is necessary to compute the initial yield moment, $M_{h(1)}$, and the deformation increment is read from the curve. In the case of curved and haunched connections, for which deflection computations are complex, the same information may be obtained by extending the elastic line to the computed initial yield moment, $M_{h(1)}$. The corresponding "theoretical" as well as the experimental rotations may be read from the graph. Included in Table 1 is a comparison of connection behavior on this basis.

(*g*) *Reduction in Moment:* Another criterion previously used in conjunction

Table 1—The Yield Strength of Connections*

		Computed initial yield moment,	Comparison of observed yield strength with initial yield moment, $M_{h(1)}$						
			Yield line moment		Visual yield moment		General yield moment		Deformation increment
Type	Connection	$M_{h(1)}$	Observed	$\frac{M_{h(1)}}{M_{h(1)}}$	Observed,	$\frac{M_{h(2)}}{M_{h(1)}}$	Observed,	$\frac{M_{h(3)}}{M_{h(1)}}$	at $M_{h(1)}$
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)
2	*A*	454	163	0.36	369	0.81	476	1.05	0.12
2B	*B*	750	305	0.41	380	0.51	600	0.84	†
15	*C*	524	402	0.77	563	1.07	645	1.23	0.13
4	*D*	597	477	0.80	477	0.80	640	1.07	0.22
	E	597	364	0.61	472	0.79	680	1.14	0.07
	F	597	264	0.44	276	0.46	640	1.07	0.24
5A	*G*	920	436	0.47	654	0.71	1125	1.23	0.07
	H	840	263	0.31	681	0.81	1036	1.24	0.08
	I	883	526	0.60	516	0.58	900	1.02	0.06
	J	620	300	0.48	475	0.77	770	1.24	0.05
8B	*K*	454	251	0.55	316	0.70	444	0.98	0.19
	L	454	276	0.61	176	0.39	432	0.95	0.19
	M	454	226	0.50	216	0.48	388	0.86	0.17
16	*N*	606	402	0.66	577	0.95	722	1.20	0.17
7	*P*								
	Shear	630	311	0.49	493	0.77	637	1.01	0.18
	Flexure	1195		0.26		0.41		0.54	†



* See text and Fig. 74 for more complete description of terminology.
† Connection did not develop $M_{h(1)}$.

with (*f*) above is the percentage reduction in moment at the computed initial yield deflection. The method is also illustrated in Fig. 77 (*b*) but has not been used in this paper.

Having described the terminology and discussed the various possible criteria, these will now be used to compare the behavior of the various connections with one another and, in some cases, with theoretically predicted values.

In Table 1 the moment at yielding, as defined by the "Yield Line," "Visual Yield" and "General Yield" criteria are compared with the initial yield moment, $M_{h(i)}$. The calculated initial yield moments $M_{h(i)}$ shown in Column 4 of Table 1 are computed from the wedge or flexure theory (as suitable) and take into account the influence of axial thrust as well as bending moment. Table 1 also includes in Column 10 the results of calculations using the "Deformation Increment" criterion (see above). In two cases (Connections *B* and *P*) the predicted yield moment was not reached by the connection.

Neglecting Connection *P*, summarizing all the tests, the three yield strength criteria give the results shown in Table 2.

Table 2—Yield Strength of Connections

Criterion	*Ratio*	*Maximum ratio*	*Minimum ratio*	*Spread (max-min)*	*Average of all readings*
Yield line moment	$\frac{M_{h(1)}}{M_{h(i)}}$	0.80	0.31	0.49	0.55
Visual yield moment	$\frac{M_{h(2)}}{M_{h(i)}}$	1.07	0.39	0.68	0.70
General yield moment	$\frac{M_{h(3)}}{M_{h(i)}}$	1.24	0.84	0.40	1.08

The "General Yield" criterion gives the least scatter and will be the basis of conclusions regarding the yield strength of the connections tested. It requires no preliminary calculations and is readily determined from the experimental curve.

Thus with respect to the prediction of the initial yield (elastic) strength it is concluded that present theories are adequate for most of the connections. As indicated by Column 9 of Table 1 most of the connections have "General Yield" values greater than the calculated initial yield moment, lower ratios being observed in connection Types 2B, 7 and 8B. This indicates that most of the connections did not yield a significant amount until a load was reached greater than the predicted value.

The points on the experimental curves that correspond to the "Yield Line" and the "General Yield" values were indicated in Figs. 19, 22, 36, 37 and 44 by "*Y*" and "*YS*," respectively.

According to the "Deformation Increment" criterion, less scatter was observed in these tests than in the case of recent continuous beam tests.[8] In the latter, the range of Deformation Increment was from 13 to 88%. In these connection tests the range of increase in deflection over the computed value at $M_{h(i)}$ is from 5 to 24%, the average value being 14% neglecting Connections *B* and *P* which did not develop strengths as high as the initial yield value.

(*1*) *Straight Knees* (*Connections A, K, L, M, P*): The straight knees indicate non-linear behavior at relatively lower loads than the rest of the connections.

Examining the "General Yield" ratio (Column 9 in Table 1) and noting the construction details, it appears that residual stress and stress-concentrations may affect the results. The number of stiffeners and hence the amount of welding increases in the order, *A-K-L-M*. In the case of Connection *M*, welding the vertical stiffener near the top would introduce tensile residual stresses. As a consequence, the application of bending moment (causing tension in the outer flange) would result in local yielding at a load lower than predicted. In the experiments the General Yield moment ratios (Table 1) increase in the order, *M-L-K* and *A*.

Difficulties were experienced with the measurement of rotation in Connections *A*, *K* and *M* in the later stages. This accounts for the heavy dashed curves in Fig. 22.

(*2*) *Tapered Haunch Knees:* Connections *B*, *C* and *N* are the largest of the tapered haunch knees tested in the program. Connection *B* had lower yield moment ratios and Connections *C* and *N* showed higher values than the average for all connections. Yield lines in *C* and *N* were observed at very nearly the same moment. In Connection *C* these lines were in the haunch web (Fig. 41) and subsequently widened as shown.

For Connections *C* and *N*, yielding in the rolled sections just outside the knee caused nonlinear behavior in the moment deformation curves. In the case of Connection *B*, however, the experimental curves deviated from a straight line almost immediately after the formation of the first yield line. The first yielding occurred at about 41% of the computed initial yield moment and "General Yield" at about 84% of that value. Residual stress probably contributed to this earlier yielding of Connection *B* since, due to the size of the assembly, there probably were large residual stresses built up due to welding.

Connections *D*, *E* and *F* were close to the average for all of the connections in their behavior. As in the case of the straight knees, those connections with full-depth stiffeners (*D* and *F*) yielded at lower moment values than the one with half-depth stiffeners (Connection *E*), Fig. 36. Undoubtedly the welding of the half-depth stiffeners induced relatively less residual stress as well as less severe stress concentration than did the full-depth stiffener.

As is indicated in Figs. 38 and 39 the bracket and stiffeners were obviously sufficient to prevent any large-scale yielding in the knee area, forcing it to occur in the rolled section. Referring also to these same two figures and to Fig. 36, the full-depth tapered stiffener as an extension of the column flange is satisfactory and the extra expense of welding a full stiffener is not justified. Use of the tapered stiffener does permit some additional yielding but this is insignificant.

(*3*) *Curved Knees:* For the curved knees *G*, *H*, *I* and *J*, Table 1 shows that the first yield line occurs at relatively lower moment than in the other connection types. However, the local yielding did not influence seriously the deformation (note that the "General Yield" ratio was higher than the average, 1.19 compared to 1.08). Due to welding, the curved inner flange should have compressive residual stresses at the edges. The formation of yield lines observed in the tests was consistent with this pattern.

As may be noted in Figs. 15 and 41, many of the built-up connections gave evidence of yielding due to shear force similar to that causing the unsatisfactory behavior of Connection *P*. However, such yielding was local in character and did not seriously influence the moment-rotation curves.

(*4*) *Moment Strength of Rolled Section at Junction with Connection:* The moment M_r at the end of the rolled section is a factor in the design problem of specifying the location of the splice or joint between the haunch and the rolled shape. As a basis for discussion and illustration the Type 2B connection will be used. Figure 78 (*a*) shows a knee proportioned to attach to a particular beam. In Fig. 78 (*b*) the initial yield moment capacity is plotted diagrammatically as a function of distance from the intersection of the extended neutral lines of the girder and column.

In a frame for which the connection is proportioned, assume that the points of inflection do not move during application of the load. This corresponds to the test condition. If the point of inflection (or load point) were at *A*, Fig. 78 (*c*), then first yielding would occur when the haunch moment equals $M_{h(i)}$; but the strength of the beam at Section 1-1 would not have been developed. In order that the girder yield strength be reached, the connection

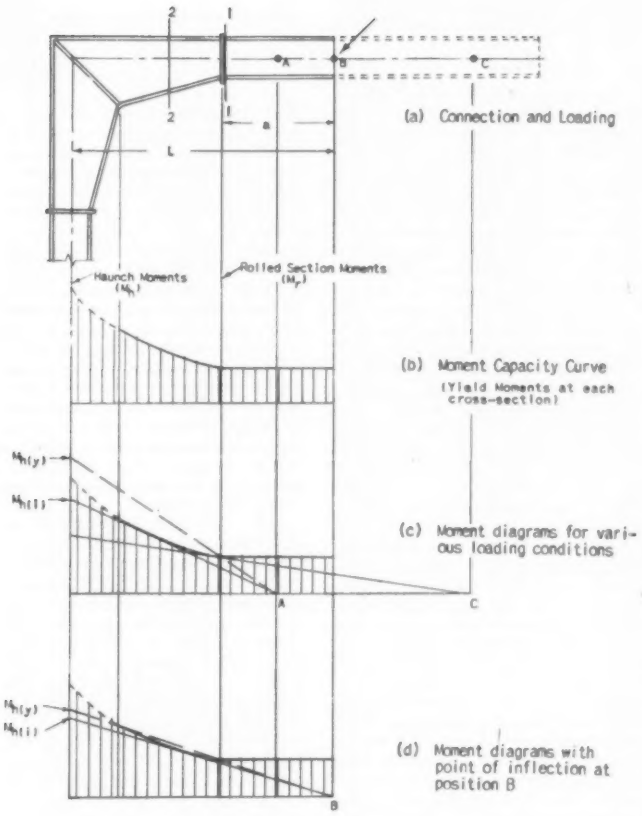

(a) Connection and Loading
Haunch Moments (M_h)
Rolled Section Moments (M_r)
(b) Moment Capacity Curve
(Yield Moments at each cross-section)
(c) Moment diagrams for various loading conditions
(d) Moment diagrams with point of inflection at position B

Fig. 78 Rolled section moment strength of Type 2B connections

would have to yield and strain harden until $M_h = M_{h(y)}$ as shown.

If the point of inflection were moved to position *B*, Fig. 78 (*d*), then the knee would yield when $M_h = M_{h(1)}$, yield occurring at a section such as 2-2. Unless a plastic hinge were developed at Section 2-2, with subsequent strain-hardening, the theoretical yield strength could not be developed at Section 1-1. Only when the point of inflection is at *C* (Fig. 78 (*c*)) is there justification for assuming that the connection will develop the elastic limit strength at the end of the rolled section.

In the testing the curved and the tapered-haunch knees the length of arm *a* was adjusted so that initial yielding should occur more or less uniformly along the length of the haunch. (This corresponds to position *B* of Fig. 78 (*d*).) A load-point at *A* was ruled out because, for static loads, good elastic design would call for haunch depth to be proportioned according to the moment diagram, the connection being stressed as uniformly as practicable along its length. It seems obvious that if the connection were to develop its yield strength at Section 1-1 with the load at position *B*, it would certainly do the same if the point of inflection were removed to point *C* or beyond.

The question then to be answered is: Did the various connections develop the initial yield moment ($M_{r(y)}$) at the junction between the haunch and the rolled section? The results of a comparison on this basis are shown in Table 3, Column 6 (*N* is not included because of the unequal flange widths). The data in Column 6 are obtained by dividing the observed "General Yield" moment (Column 4, defined previously) by the haunch moment at theoretical yielding of the rolled section, Column 5. The data in Column 5 has also been corrected for variation in material properties between the material used to fabricate the gaunches and the 8B13 rolled shape. The variation is shown in Table 4. Two values are shown for Connection *C*, Table 3. This is because the connection is not symmetrical and

Table 3—Comparison of Observed General Yield Strength with Rolled Section Yield

Type	*Connection*	L/a*	*Observed general yield moment,* $M_{h(2)}$	*Computed haunch moment at yield of rolled section,* $M_{h(y)}$	$\frac{M_{h(2)}}{M_{h(y)}}$	*Comparison neglecting influence of axial load,* $\frac{M_{h(2)}}{M'_{h(y)}}$
(1)	(2)	(3)	(4)	(5)	(6)	(7)
2	*A*	1.13	476	454	1.05	0.97
2B	*B*	2.10	600	771†	0.78	0.66
15	*C*					
	a-a	2.04	645	791	0.82	0.73
	b-b	1.31		524	1.23	1.13
4	*D*	1.53	640	597	1.07	0.97
	E	1.53	680	597	1.14	1.02
	F	1.53	640	597	1.07	0.97
5A	*G*	2.69	1125	976†	1.15	0.96
	H	2.50	1036	886†	1.17	0.95
	I	2.50	900	771†	1.17	0.83
	J	2.03	770	720†	1.07	0.87
8B	*K*	1.13	444	454	0.98	0.90
	L	1.13	432	454	0.95	0.88
	M	1.13	388	454	0.86	0.79

* L = distance from inflection point to haunch point, H; a = distance from inflection point to junction of beam and connection (Fig. 78).

† Adjusted for variation in material properties between haunch and rolled section.

Table 4—Strength of Connection Material

Material	*Yield point stress in tension, ksi*
8B13 flanges	41.8
1/2-in. plate (*B*, *C*, *G*, *J*)	39.1
3/8-in. plate (*H*, inner flange only)	39.0
1/2-in. plate (*I*, inner flange only)	35.2

yielding would occur sooner at one end of the connection than at the other.

The following observations are made with respect to the data in Column 6 of Table 3.

(*a*) Nearly all of the connections developed "General Yield" strengths greater than a moment corresponding to initial yield at the end of the connection.

(*b*) The straight knees had lower ratios than most of the other connections.

(*c*) Connection *B* yielded at 78% of the load corresponding to rolled section yield. Based on Section *a-a*, Connection *C* yielded at 82% of the load corresponding to rolled section yield. Under the symmetrical loading system used Section *b-b* would be the critical cross section of the connection.

(*d*) As a group, the curved knees give the best performance.

In Column 7 of Table 3 the connections have been compared neglecting the influence of additional direct stress due to axial load in the computation of $M_{k(y)}$. In all but two cases significant yielding occurred at loads lower than the initis' yield load in flexure at the junction of rolled beam and connection.

3. *Stiffness of Connections in the Elastic and Plastic Range*

This section will cover the load-deformation aspects of connection behavior, including the moment-rotation and moment-deflection curves. Both the elastic and initial inelastic behavior will be considered.

In Fig. 52 moment at the knee has been plotted against the average unit rotation (total rotation in the knee divided by the equivalent length). These are all experimentally determined curves. The solid line is the curve determined from the control-beam test (simply supported beam under third-point loading).

This figure shows that all of the so-called "built-up" connections exhibit an average stiffness greater than that of the rolled section (8B13) in the elastic region. Only the straight knees (Types 2 and 8B) are less rigid than the rolled section. It has been observed in tests of continuous beams[24] that residual stresses and stress concentrations may cause an increase in deflections above predicted values in the so-called elastic region; but the increased deformations in the connection tests are somewhat greater than those usually found in beam tests.

The increased rotations in the straight knees will increase the bending moments and deflections elsewhere in any frame of which the knee is a part. In a frame with a column height of 10 ft and a beam span of 24 ft loaded at the third-points, using the 8B13 section and the experimental rotation of Connection *L* at the theoretical initial yield moment, the deflection was found to be about 10% greater than predicted. This was due to the increase in connection rotation above the value predicted on the basis of complete continuity.

3A. *Straight Knees*

Connection P (Type 7): In Fig. 19, describing the behavior of Connection *P*, the "computed elastic stiffness" considering both shear and flexure, is about 13% greater than the measured slope of the experimental curve (solid line). The theoretical moment-rotation curves for the 8- and 14-in. members are shown by broken lines. On the basis of "equivalent length" (see Part II) the elastic curve should lie between these two curves; it is shown in Fig. 19 as a long dashed line. The connection fails by a factor of two to develop elastic stiffness equivalent to that assumed in elastic design; thus elsewhere in a structure of which it was a part, stresses would be higher than the computed values.

At a moment of about 600 in.-kips, the connection commences to yield rapidly (due to shear in the knee panel) but at the same time it continues to carry increased bending moment. The increase in load is due to the fact that (*a*) there is strain hardening in the web and additional load is required to cause yielding in those parts of the knee removed from the haunch point, and (*b*) the flanges provide restraint by bending as shown in Fig. 21

From the discussion in Part II, it is evident that a calculation could be made to obtain the required thickness of web material such that a Type 7 knee will meet the stiffness requirements. This requirement for knees joining rolled sections of equal depth was that the rotation, ϕ_A, measured over an equivalent length ($\Delta L = d$) be no greater than that given by the expression

$$\phi_A = \phi d \qquad (40)$$

where

$$\phi = \frac{M_r}{EI} = \frac{M_k}{EI}\left(1 - \frac{d}{2L}\right).$$

From eqs 16 and 17 of Part II, the sum of the shear deformation, γ_r, and the bending deformation, β_r, is given by

$$\gamma_r + \beta_r = \frac{M_k}{wd^2G}\left(1 - \frac{d}{L}\right) + \frac{M_k}{2EI_F}\left(1 - \frac{d}{2L}\right)d \qquad (41)$$

The required web thickness is obtained by equating expressions 40 and 41. Further, neglecting the influence of the terms $[1 - (d/L)]$ and $[1 - (d/2L)]$,

$$\frac{M_k}{EI}\cdot d = \frac{M_k}{wd^2G} + \frac{M_k d}{2EI_F}$$

Replacing I_F by I (an assumption on the unsafe side by a small amount),

$$\frac{d}{EI} = \frac{1}{wd^2G} + \frac{d}{2EI}$$

Then the required web thickness, w, is given by

$$w = \frac{2EI}{d^2G} = \frac{ES}{d^2G}$$

and for $E = 30 \times 10^6$ psi, $G = 11.5 \times 10^6$ psi,

$$w \geq 2.6\frac{S}{d^2} \qquad (42)$$

This expression gives a web thickness, w, which will provide adequate stiffness in the elastic region. When measured rotations for Connection *P* are compared with values computed according to this analysis, agreement within 10% if observed.

Connection A (Type 2): The moment-rotation curve for Connection *A* is shown in Fig. 22. The two theoretical curves in the elastic range are for the two different assumptions made in Part II of the paper. The dot-dash computed elastic line takes into account both shear and flexure; the dotted "equivalent length" line considers only flexure of an equivalent length of rolled beam. It is evident from the rotation calculations of Part II that both the deformation due to shear and to flexure must be considered when computing knee rotations.

Below 160 in.-kips Connection *A* deformed only 9% above the predicted value. Larger than predicted rotations would be expected due to the absence of stiffeners to transmit the reaction at the re-entrant corner of the connection. The measured rotation is 38% greater than the theoretical value at a moment of 400 in.-kips. It was noted previously that rotation measurements for Connection *A* were unreliable in the higher ranges.

Connections K, L, M (Type 8B): Using Connection *L* as the basis for comparison, the experimental curve in the elastic range indicates rotations only about 1% greater than that predicted considering both shear and flexure (dot-dash line in Fig. 22). Thus excellent agreement between theory and test was obtained.

The simplest computation of rotation, that based on "equivalent length" as developed in Part II, predicts rotations that are about 14% less than the observed values. Thus this connection type does not have sufficient effective web thickness to prevent elastic rotations greater than those assumed by the simple theory. It would be of value to see how well the simple theory (which neglects shear) agree with tests of other sizes and shapes of cross section.

In Fig. 22 in the inelastic region, two "theoretical" moment-rotation curves are drawn, one for uniform moment throughout the equivalent length of the knee, the other based on the assumption that no inelastic rotation occurs within the knee (see Part II). The first assumption (dotted curve) provides the best agreement with experiment over the greatest range. At the end of the elastic portion and in the initial plastic range experimental values are greater than predicted by theory. Factors which contribute to this are residual stresses, stress concen-

trations, and a certain amount of yielding due to shear force at the higher moments.

Deflections: Figure 23 shows the deflection curves of the four straight knees (*A*, *K*, *L*, *M*). The theoretical curve (dashed line) is based on assumptions outlined in Part II resulting in eqs 33–35. The experimental deflections are greater than predicted by the theory used since the latter is based on so-called minimum requirements that do not take shear deformation into account. This deflection computation assumes uniform moment over an equivalent length of knee. A more exact comparison with the deflection curve in the elastic region could undoubtedly be obtained by the use of the "exact" predicted rotation developed in Part II.

Due to stress concentrations, residual stress and plastic deformation due to shear force the experimental curves become nonlinear at relatively low loads. However, the increase in deflection does not become uncontrolled until a load is reached corresponding approximately to the plastic hinge moment at the end of the rolled section.

Figure 23 also shows the theoretical curve in the strain-hardening region. Connection *L* was provided with the best lateral support and the agreement between theory and experiment is good.

3B. Tapered Knees

A theoretical analysis of the tapered and curved knees is not included in this report. A few comments on stiffness of the two types in the elastic and plastic range will be made.

The use of any of the tapered knees tested in this program as part of a frame would assure a continuity at least as great as that implied in the assumption of complete continuity in straight knees (Fig. 52). Neglecting Connections *N* and *D*, the measured elastic stiffnesses of the connections fall within a narrow band more than twice the stiffness of the rolled section.

As shown in Fig. 36 there is a difference in behavior of Connection *E* when compared with *D* and *F*. The latter with the full-depth stiffeners at the bracket ends show increased deformations in the initial plastic range as compared with Connection *E* which has half-depth stiffeners. It is suggested that this is due to residual stresses induced due to welding of the full-depth stiffeners (*D* and *F*) in the vicinity of the tension flange as has been discussed earlier. The half-depth stiffener thus appears to have a slight advantage.

There may be a rough correlation (based on the simplest of assumptions) between average unit rotation and minimum distance, d_h, measured from external corner to the inner flange at the haunch (Fig. 79). The average unit rotation, ϕ, of a beam under constant moment and with constant modulus of elasticity varies inversely as I. If the web is neglected, then I varies as d^2 and

$$\frac{\phi_1}{\phi_2} = \frac{I_2}{I_1} = \frac{d_h^2}{d'^2}$$

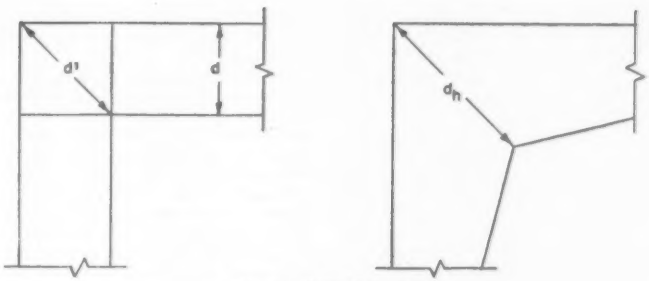


Fig. 79 Haunch depths

where

ϕ_1 = average unit rotation of beam with depth d' ($d' = 1.414d$, Fig. 79).

ϕ_2 = average unit rotation of beam with depth d_h (Fig. 79).

Comparing Connection *B* with the rolled section curve, the computed ratio of stiffness is

$$\frac{\phi_1}{\phi_2} = 2.22$$

Examining Fig. 52 at $M = 270$ in.-kips, the observed unit rotation ϕ_a of rolled beam of depth d is $\phi_a = 0.0002$ rad. Then ϕ_b, the computed rotation of Connection *B* should be

$$\phi_b = \frac{\phi_2}{\phi_1}\phi_a = 0.00009 \text{ rad.}$$

The experimental average unit rotation for Connections *B*, *C* and *F* from Fig. 52 is $\phi_b = 0.000075$ rad. This discrepancy between computation and experiment is 17%. (Taking the influence of the web into account the difference between experimental results and calculated value is about 5%.) Thus, the stiffness of these haunched connections compared to the rolled section varies roughly as the square of the minimum distance measured between the external corner and the inner flange.

3C. Curved Knees

Connection *J* was intended for comparison with Connections *D*, *E* and *F*. The curves in Fig. 36 show that the elastic stiffness of all four connections is almost identical. The stiffness of curved connections as indicated by total rotation measurements (not shown in the figures) is in decreasing sequence *I*, *H* and *G*. The deflection measurements (Fig. 44) show similar behavior since the lengths of the rolled sections vary about the same as the total length of test specimen. This behavior is expected since the connections with larger equivalent lengths and thinner curved flanges would be expected to give larger total rotations. If, however, the total rotation is divided by the equivalent length to give the average unit rotations, Connections *G*, *H* and *I* have very nearly the same elastic stiffness, *G* being somewhat more rigid. Connection *J* is considerably more flexible than the other curved knees as indicated by average unit rotation (Fig. 52). The three connections *G*, *H* and *I*, proportioned approximately according to the AISC procedures,[1] have a stiffness averaging about three times that of the rolled section.

The correlation between stiffness and haunch depth, d_h, suggested earlier for tapered knees is fairly good for the curved knees. Assuming that I varies as d_h^2, at constant moment $M = 270$ in-kips the measured and computed values agree within 10% as shown in Table 5. Only the two connections with equal thickness of inner and outer flanges are included.

Table 5—Comparison of Connection Stiffness on the Basis of Haunch Depth

Connection	Average unit rotations, ϕ, in radians — Observed	Computed	Difference, %
J	9.6×10^{-5}	9.0×10^{-5}	6.2
G	4.9×10^{-5}	4.5×10^{-5}	8.9

Thus, for the curved and tapered knees with equal thickness of inner and outer flanges, the average unit rotation, ϕ_h, may be determined approximately by the expression

$$\phi_h = \frac{2d^2}{d_h^2}\frac{M_h}{EI} \qquad (43)$$

where

d = depth of rolled beam.

d_h = minimum distance from external corner to inner flange.

M_h = haunch moment.

It is emphasized that the above expression is developed from the grossest of assumptions and may be coincidental to these tests.

4. Plastic Strength and Load-Deformation Characteristics Beyond the Maximum Load

4A. Introduction

In Part I of this paper the requirements for connections were discussed from the point of view of plastic design. Three such requirements were noted as follows:

(1) Straight connections must be capa-

ble of resisting at the corner the full plastic moment, M_p, of the rolled sections joined.

(2) For straight knees the stiffness (or "rigidity") should be at least as great as that of an equivalent length of the rolled sections joined.

(3) The connection may be required to absorb further rotations at near-maximum moments after reaching the plastic hinge condition. This property has been termed "rotation capacity."

No special requirement was listed for haunched knees since it is not clear that plastic design is suited to frames with built-up connections. To give adequate factor of safety and to justify assumptions in analysis, requirements (1) and (2) above are also desirable properties for elastic design.

The stiffness of connections has been discussed in the previous section. If deflection is not a matter of great concern, increased flexibility could be allowed in the elastic range so long as the connection eventually developed the required strength. A special examination of a frame might be required in this case to make certain that at the last plastic hinge point there was adequate rotation capacity to counteract the influence of large deformations at a connection. On the other hand, if deflection is critical in the design (as may well be the case), then rotations beyond those implicitly assumed in design computations could have an adverse effect.*

The observed behavior of all the connections follows the same general pattern. The initial elastic range is followed by the elastic-plastic stage (an initial plastic region) in which the rotations and deflections gradually become larger for equal increments of load. After a gradual increase in the amount of yielding, local plastic instability of the compression flanges is observed, tending to cause the knee to buckle in a direction normal to the plane of the knee. With increasing loads the local buckling becomes more pronounced, and the connection collapses soon after plastic buckling occurs in the web.

In Table 6 the maximum plastic moments are compared with various computed moments for the different connections. As this table is studied with Fig. 74, which describes the terminology, it will be seen that each criterion of strength (plastic hinge at the haunch, initial yield, rolled section yield and plastic hinge at the splice) is successively more severe. In considering the plastic strengths, then, these four criteria form a basis for the discussion.

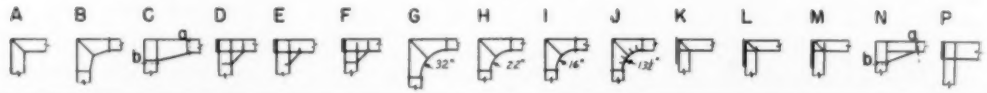
* The problem of plastic design and deformation has been discussed in Progress Report No. 3.[5]

(*a*) *How does the observed maximum haunch moment $M_{h(s)}$ compare with the plastic hinge moment of the rolled section, $M_{h(s)}$?* Column 4 of Table 6 shows that all of the connections except Connection *P* developed the predicted plastic hinge moment $M_{h(s)}$ at the intersection of the neutral lines of column and girder. It is rather obvious that built-up connections would be more than adequate in this respect since so much additional material is supplied at the haunch. It would certainly be wasteful of material if haunches were used only to assure the development of plastic hinge strength.

(*b*) *How does the maximum observed haunch moment compare with the computed initial yield moment, $M_{h(i)}$?* This important general comparison, suggested by T. R. Higgins, is shown in Column 6 of Table 6. Excepting Connection *P* (whose deficiencies have already been discussed) and Connection *B*, the knees exhibited a reserve strength beyond the elastic limit greater than that of a simply supported beam under pure bending. The ratios range from 1.21 to 1.47; for the pure bending of a beam, the ratio of the maximum load carried compared to the load at which the test member is computed to yield (this ratio is called the "shape factor") ranges from 1.12 to 1.20 and for the 8B13 shape is 1.15.

Table 6—The Maximum Plastic Strength of Connections*

Type	Connection	Comparison with computed plastic hinge moment, $M_{h(s)}$ = 517-in. kip: Observed maximum haunch moment, $M_{h(s)}$	$\frac{M_{h(s)}}{M_{h(s)}}$	Comparison with initial yield moment, $M_{h(i)}$: Computed, $M_{h(i)}$	$\frac{M_{h(s)}}{M_{h(i)}}$		Comparison with computed haunch moment at yield of the rolled section, $M_{h(y)}$: Computed, $M_{h(y)}$	$\frac{M_{h(s)}}{M_{h(y)}}$	Comparison of observed maximum rolled section moment, $M_{r(s)}$ with computed plastic hinge moment, $M_{r(p)}$†: Observed maximum moment in rolled section, $M_{r(s)}$	$\frac{M_{r(s)}}{M_{r(p)}}$
(1)	(2)	(3)	(4)	(5)	(6)		(7)	(8)	(9)	(10)
2	*A*	548	1.06	454	1.21		454	1.21	486	0.94
2B	*B*	728	1.41	750	0.97		771‡	0.94	346	0.67
15	*C*	720	1.39	524	1.37	*a-a*	791	0.91	353	0.68
						b-b	524	1.37	555	1.07
4	*D*	751	1.45	597	1.26		597	1.26	492	0.95
	E	804	1.55	597	1.35		597	1.35	526	1.02
	F	844	1.63	597	1.41		597	1.41	552	1.07
5A	*G*	1296	2.50	920	1.41		976‡	1.33	482	1.00‡
	H	1228	2.37	840	1.46		886‡	1.38	490	1.02‡
	I	1180	2.28	883	1.33		771‡	1.53	472	1.08‡
	J	915	1.77	620	1.47		720‡	1.27	452	0.93‡
8B	*K*	560	1.08	454	1.23		454	1.23	496	0.96
	L	624	1.21	454	1.37		454	1.37	553	1.07
	M	576	1.11	454	1.27		454	1.27	511	0.99
16	*N*	894	1.73	606	1.47	*a-a*	...	...	390	0.75
						b-b	...	...	591	1.14
7	*P*									
	Shear	1150	0.96	630	1.84		...	...	1100	0.92§
	Flexure			1195	0.96					



* See Fig. 74 for terminology.
† $M_{r(p)}$ = 517 in.-kips for all connections except *P*, in which $M_{r(p)}$ = 1190 in.-kips.
‡ Adjusted for variations in material properties between haunch and rolled section.
§ This ratio is based on 1190 in.-kips, the M_p of 8WF31.

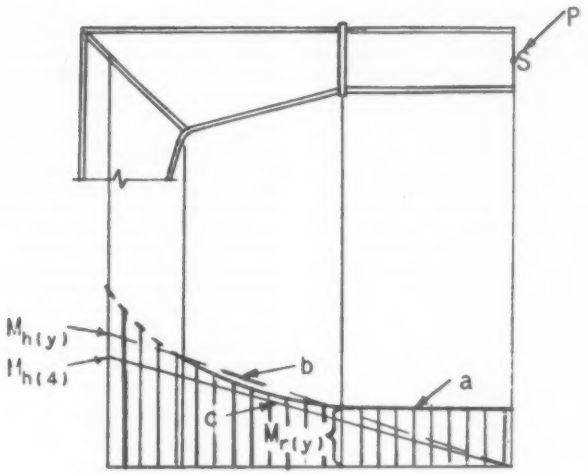


Fig. 80 Theoretical and experimental moment curves

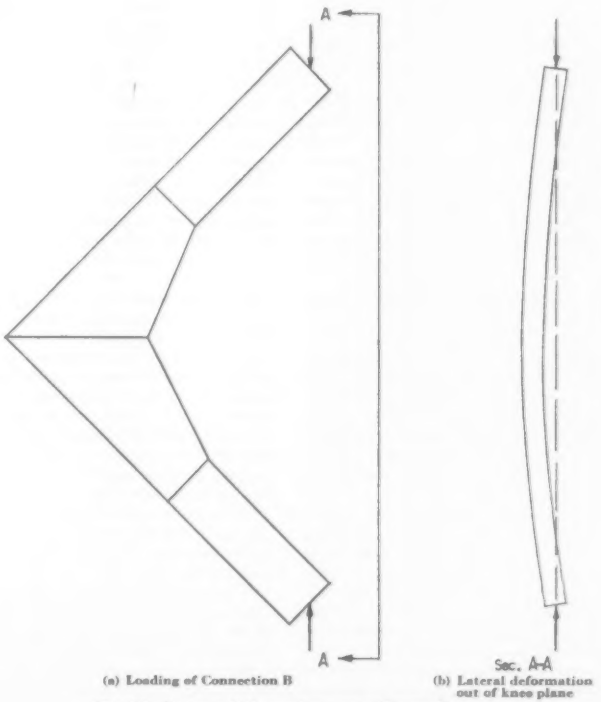
(c) *How does the maximum observed haunch moment compare with the predicted yield moment of the rolled section, $M_{h(y)}$?* The results of this comparison are shown in Column 8 of Table 6. Although it has been seen in an earlier part of this paper that all of the connections yielded at moments less than a value equivalent to the yield moment at the splice, all of the connections eventually developed strengths greater than this value except Connection *B* and one arm of Connection *C*.

Since the compression flange of Connection *B* buckled before the full strength of the beam had been realized, the inner flange should be made thicker. With this increased thickness, one would expect improved moment-rotation characteristics since yielding would occur at locations other than along the compression flange. Recognizing the improvements that should be made in designing Type 2B knees, the behavior of this particular test Connection *B* should be regarded as a special case. In Fig. 80 the initial yield moment capacity of the connection at each cross section is given by line "*a*." The load was at position *S* and thus the theoretical moment distribution corresponding to the initial yield condition at the splice, $M_{r(y)}$, is shown by line "*b*." The actual moment diagram at collapse is given by line "c_y" where $M_{h(4)}/M_{h(y)} = 0.94$.

Two other factors probably influenced the behavior of Connection *B:* the method of lateral support and residual stress. As will be remembered, Connection *B* was the first of the larger built-up knees to be tested and the vertical guide system of lateral support was used (Fig. 15). This scheme lacks the stiffness inherent in the direct tie bars used on the later curved knees (Fig. 16). Figure 81 (*b*) shows the lateral deformation that occurred normal to the plane of the knee after the connection had yielded, indicating insufficient support stiffness. Further, the tendency toward lateral buckling will be greatest in this connection type. Because of the loading and shape of knee a considerable length of it yields at the same load. On the other hand, in the curved knees yielding is concentrated in one area (compare Fig. 40 with Fig. 49 for an example). Buckling of the inner compression flange should occur when the flange becomes plastic. The longer the portion of haunch that is yielded, the more severe will be the buckling tendency.

The second factor influencing the behavior of Connection *B* is residual stress. This factor undoubtedly is present in curved knees. Knees completely built-up by welding, contain residual stresses which are probably of similar form to those induced when a rolled shape cools after rolling. Tensile stresses would be formed along the line of the weld and compressive stresses would be present at the flange edges. When the inner compression flange yields due to the residual stress, then its buckling strength is reduced and lateral deformation such as that shown in Fig. 81 (*b*) would occur. More studies of the Type 2B connection are planned including some in which compression flanges are supplied with additional thickness.

(d) *How does the maximum observed rolled section moment $M_{r(4)}$ compare with the computed plastic hinge moment at that point, $M_{r(p)}$?* This comparison is shown in Column 10 of Table 6. The straight knees (Types 2 and 8B) and the connections with the 45-deg haunch were satis-



(a) Loading of Connection B

(b) Lateral deformation out of knee plane

Fig. 81 Lateral deformation out of knee plane

factory. The Type 7 straight knee did not quite develop the yield strength (although it had good rotation capacity). One of the curved knees (Connection *J*) and the tapered Connection *B* were unable to deform plastically and still maintain their cross-sectional form sufficiently to develop the rolled beam hinge moment at the splice. Thus it would appear that it is not possible to extend the plastic design method to such an extent that the position of the splice between beam and connection is selected on the basis of the predicted plastic hinge moment at that point. If such a procedure *were* to be followed, sufficient thickness of material must be placed in the inner flanges so that yielding will occur in the rolled section at the splice and not within the knee itself.

As noted earlier, it is not clear at this time that plastic design is suited to frames with haunched connections, although there are certain possibilities which will be mentioned later on.

Before discussing in detail the strength of the various connections the problems of plastic instability and lateral support and of rotation capacity will be mentioned.

4B. Plastic Instability and Lateral Support

Commercially available cross-sectional shapes such as those used in this program are proportioned in such a manner that local buckling of flange or of web elements does not occur in the elastic range. Similarly the members and complete connections were of short enough lengths that elastic lateral buckling was not possible.

However, once the elastic limit is passed (and residual stresses may cause this situation at lower than expected loads) both local and lateral buckling follow since, in the yielded area, the tangent modulus is reduced from about 30,000,000 psi to a value approaching zero. The fact that immediate collapse does not occur may be attributed to the yielding process for structural steel.[11] The subject of local plastic instability is being currently studied at Lehigh University.

It is emphasized, however, that *plastic instability was involved in the collapse of every connection tested, and in most cases brought about final collapse.* Similar connections joining members of other WF shapes would be expected to perform at least as well since the light 8B13 section has low resistance to local instability in the inelastic range of stress.

The onset of yielding results in a marked reduction in stiffness; this, of course, necessitates the use of lateral support in a test program. The three methods used for providing lateral support were described in Part I of this paper. The results of these tests, together with others, indicate that for members with cross-sectional form similar to the 8B13 shape, the flex-bar support system is much better than the vertical guide system. The latter introduces friction when the tendency to lateral deformation begins. Furthermore, the vertical guides, being more flexible, allow more lateral deflection which in turn further aggravates the buckling tendency. Local and lateral inelastic buckling start at very nearly the same time. For straight knees this may be seen by reference to Figs. 30 and 31.

The improved support provided by the flex-bars as against the vertical guide system is clear from a comparison of the final buckled shape of Connections *B* and *G*. Lateral support on *G* (flex-bars) was adequate to cause collapse to occur in an *S*-shaped pattern (Fig. 46); Connection *B*, on the other hand, buckled in a single half-wave, Fig. 81. Figure 52 shows that the result of improving the lateral support is to increase the rotation capacity (compare *D* and *E* with *F*, and compare *A* and *K* with *L* and *M*).

It was confirmed in this investigation that a rather small force is required initially to prevent sidewise deformation. However, when the flange elements buckle locally, tending to cause lateral buckling, then this force increases rapidly (Figs. 30 and 43). Bracing at the knee for connections of this type would be adequate in the plastic range if it could carry about 10% of the total thrust on the knee and were tied to a rigid supporting structure.

Connections *M* and *L*, having about the same strengths, provide further opportunity for examining the influence of lateral support. Whereas *M* commences to lose the ability to carry load after a deflection of about 0.7 in. (Fig. 23), *L* continues to carry increased load. The importance of adequate lateral support is evident since Connection *L* has shorter stiffeners than Connection *M*. Comparing Connections *F* and *D*, Fig. 36, the latter has the most effective web stiffening, but *F* develops greater strength. Effective lateral support is the only explanation for the greater plastic strength since the load-deflection curves are identical in the elastic and early plastic region.

It is concluded that effective lateral support is more significant than variation in fabrication details in so far as plastic strength of connections is concerned. Although, for the two connection types compared in the above paragraph, inferior lateral support did not prevent the members from reaching the predicted load, the more positive support in each case increased carrying capacity and, in particular, the rotation capacity.

To be most effective it is evident that lateral support must be provided as close to the expected point of yielding as possible. Referring to Fig. 52, the built-up knees may be compared as a group with the straight knees (*A*, *K*, *L*, *M*). In the latter the point of lateral support, which is the re-entrant corner, coincides with the point at which flange yielding will first occur. Support is thus provided at the location at which it is needed the most. However, in the haunched knees (*B*, *G*, *H*, *I*, *J*) lateral support cannot be provided along the whole length of inner flange. In the tests local buckling occurs at locations remote from the point of lateral support and as a consequence collapse is relatively rapid. This behavior is also seen within the group of curved knees themselves. Connections *I* and *J* have the shortest effective lengths and possess better plastic rotation characteristics than connections *G* and *H* which buckle more rapidly. In haunched connections it would appear that lateral support should be provided at the end of the haunches (splice points) and also at the mid-length of the inner flange. Use of a channel shape formed to fit the inner flange has been suggested to improve resistance to lateral buckling.

4C. Local Buckling and Rotation Capacity

The moment at which visible local buckling of flange elements occurred in each of the connections has been shown in Fig. 52. The designation of two observations for Connection *N* corresponds first, to buckling of the column flange and, second, to local deformation of the beam flange. In the straight knees (*A*, *K*, *L* and *M*) the moment at first observed local buckling was nearly identical in all the tests. (Figure 22 shows these data to a larger scale.) Local buckling occurred at an average unit rotation of about 0.00033 radians per inch in curved and haunched knees.

Referring to Fig. 22, local buckling is indicated at two places for Connection *L*. One is at a moment at 520 in.-kips which corresponds to local buckling of the girder flange. At a moment of 588 in.-kips the column flange buckled, a higher moment being required due to the extra stiffening supplied to this flange by the end plate. Beyond this point, rotations increased very rapidly although the connection continued to carry load.

These tests show that local buckling is followed almost immediately by a significant increase in deformation per unit of load increment and in some cases by almost immediate collapse. For plastic design, it would be essential that a specification be developed for proper geometric proportions of rolled shapes to prevent premature inelastic buckling. A study of this is included in the current program at Lehigh University mentioned earlier.*

In Part I of this report, in the section "Requirements for Connections" the importance of adequate rotation capacity in the case of straight knees was emphasized as follows: "The knee (straight) may be required to absorb further rotations at near-maximum moments after

* Plastic local buckling of outstanding flanges is a problem that apparently has not concerned investigators in the British Isles and on the continent of Europe. The reason probably lies in the fact that the I-shape has inherent resistance to local buckling that the WF shape does not possess. Since the latter is not in general use in those areas, the problem has not required serious attention.

reaching the plastic hinge condition. This property has been termed 'rotation capacity.' The precise requirement depends on degree of restraint, the loading and the length-depth ratio of the portal beam." A rotation capacity of about eight times the total rotation at initial yield was suggested for a uniformly loaded beam fixed at the ends with a length-depth ratio of 30. For portal frames under vertical loading this constitutes a rather severe requirement since the girder ends are not completely restrained. Indeed, if the loading were such that in the elastic region the center and connection moments were nearly equal, then theoretically no rotation capacity would be required to develop the predicted ultimate load-carrying capacity. In elastic design there is no requirement for a reserve rotation capacity although ductility is desirable as a safeguard against brittle failure. A specified rotation capacity is a requirement peculiar to plastic design.

Local buckling followed by lateral instability is the phenomenon that most directly limits the ability of a connection to rotate under constant moment. If local instability did not occur, then, after reaching the plastic hinge condition, the connection would merely rotate under nearly constant moment, a condition assumed by the simple plastic theory and the plastic design method based upon that theory. On the other hand, the occurrence of the first local buckling does not necessarily result in immediate collapse and loss in moment capacity of a connection. As confirmed by the tests, the two factors of predominant importance are the geometric proportions of the cross section and the efficiency of the lateral support system. Connections with the deepest haunches and thinnest flanges (*G* and *B*) have the poorest rotation capacity (Fig. 52). The rotation capacity of Connections *H* and *I* with relatively thick flanges of small radius is better in each case than that of Connection *G*. See Table 7. Connection *J* has good rotation capacity and the intermediate flange stiffeners are probably helpful in this respect. The straight knees with the smallest haunch depth have the best rotation capacity. None of the built-up connections have rotation capacities as large as straight knees *L* and *M*.

Table 7—Radius and Thickness of Curved Inner Flanges

Connection	*Radius, in.*	*Thickness, in.*
G	32	1/4
H	22	3/8
I	16	1/2

The efficiency of the lateral support system was the second factor mentioned as governing the rotation behavior of connections after local buckling occurs. In the straight connections the maximum flange stress is at the re-entrant corner. As was mentioned earlier this is consequently the region at which local buckling will first occur. This is also the point at which lateral support is provided (this was done most effectively in the case of Connection *L*), and it will be seen that local buckling was not followed by immediate lateral buckling and collapse (Fig. 22). If adequate lateral support is provided at the point at which local buckling is expected, then local buckling is symmetrical about the web as shown in Section *A-A*, Fig. 82. There is little tendency toward lateral buckling. However, when later plastic deformation occurs at cross sections removed from the point of lateral support local buckling commences on one side of the flange (Fig. 82, Section *B-B*) inducing lateral motion, the web deforms, and the connection collapses. Connection *L* is an example of this behavior. Figure 29 shows the pattern of local buckling on one side of the connection. Referring also to Figs. 82 and 22, symmetrical local buckling in the girder flange was observed at a moment of 520 in.-kips and in the column flanges at a moment of 588 in.-kips. Unsymmetrical local buckling did not occur until the moment reached 616 in.-kips at a rotation greater than the maximum shown in Fig. 22. This was near the maximum moment and collapse followed shortly thereafter. As was stated earlier, it is not feasible to provide lateral support in built-up connections at every point at which local buckling is to be expected. Thus the local buckling is not symmetrical in the haunched connections and they are unable to maintain their maximum moment strength through further rotations.

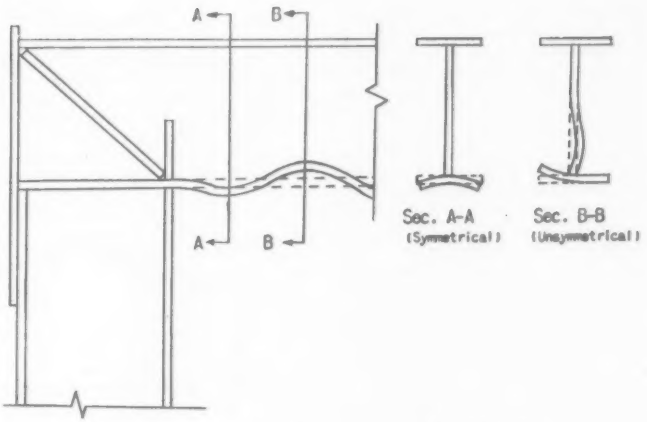


Fig. 82 Symmetrical and unsymmetrical local buckling

Rotation capacity is improved by the use of stiffeners which maintain the cross-sectional shape and prevent deformation of the web. Connections *A*, *K* and *M* were all tested with the vertical guide lateral support system. Connection *M* exhibited good rotation capacity; although a "collapse curve" was not determined for Connections *A* and *K*, it was observed that the load was dropping off rapidly. Of these three connections, only *M* was supplied with a stiffener in way of the column flange. The much greater rotation capacity affords some evidence that such a stiffener constitutes an improvement in design. Figure 26 shows the manner in which yielding and web crippling tend to occur directly outside of the flange extensions. Thus the effect of vertical and horizontal stiffeners (flange extensions) is to prevent local web buckling directly over the flange, the cross-sectional shape being maintained, thus increasing the rotation capacity. The use of numerous stiffeners normal to the flange accounts at least in part for the ability of Connection *J* to maintain its moment strength through a considerable rotation after the elastic limit was passed (Fig. 52).

In summary, then, lateral support should be provided where local buckling is expected, the latter occurring at points of maximum stress. The provision of stiffeners normal to the compression flange at points where buckling is expected will assist in improving rotation capacity if spaced sufficiently close together.

The collapse of the tapered and curved haunched connections after reaching the maximum moment may be more sudden than the rest of the connections. When used in frames designed in accordance with elastic principles, this tendency toward sudden collapse is of no particular significance. As mentioned in Progress Report No. 3, whenever a structure can be efficiently designed by elastic methods (where depth of section may be varied to suit the moment diagram) there is doubtful advantage of plastic methods.

However, if the built-up connections are sufficiently stable after the maximum

moment has been reached, it is conceivable that one might make use of this behavior in plastic design. A frame might be proportioned for one loading condition, and the plastic behavior at the connections might result in a structure just as safe for some other distribution of load. As a result, appreciable savings of material might be realized. Suppose, for example, that two critical conditions of loading on a portal frame result in the moment diagrams of Fig. 83 (*a*) and 83 (*b*). The sections *BCD* are reproduced to a larger scale in Figs. 83 (*c*) and (*d*) respectively. According to elastic design procedure the frame would be proportioned on the basis of a rolled shape adequate for axial thrust plus a bending moment of magnitude M_1. The haunch would extend into the frame a sufficient distance to make $M_2 = M_1$. In the example, this would require a haunch extending about $6^1/_2$ ft along the girder (distance Y in Fig. 83). If, now, the haunch had adequate rotation capacity it might be shortened in length, the moment diagram of Fig. 83 (*c*) being used as the basis for determining the distance "X" such that $M_2 \leq M_1$. Providing that $M_4 + M_5 \leq M_1 + M_3$, then the frame proportioned for the moment diagram of Fig. 83 (*a*) would carry loads which result in the diagram of 83 (*b*). There would be a resultant savings of material and fabrication cost due to the shortened haunch length, amounting to about three feet in this example. In the above expression, if $(M_4 + M_5)$ is greater than $(M_1 + M_2)$ then the deficiency could be made up by selecting a slightly heavier rolled shape or selecting the haunch length, X, such that an equality is realized.

Further illustration of the possible use to be made of plastic behavior is in the case of foundation settlement. Consider the two-span frame shown in Fig. 84 loaded uniformly. According to elastic design, haunches would extend to positions "*a*" and "*b*" ($ac = ef = bd$). In the event of settlement of the center support, a redistribution of moment would occur similar to that shown in Fig. 84 (*b*). There would be overstress at the haunch at *B*. However, if this haunch had adequate rotation capacity, then the frame would not be in danger; under the "full load"* there would merely be a further redistribution of moment that would tend to increase again the moment at *C*. Similar behavior would be observed in case the foundation at *A* were the one to settle.

If haunches in multiple span single-story frames could be demonstrated to develop plastic hinges, this would be of further advantage since design procedures might thereby be simplified. Such a procedure could take the form of an almost arbitrary proportion of haunch length for architectural considerations with the rolled shape selected to carry the remaining simple span moment diagram (*c*, *e*, *d* in Fig. 84).

* Working load multiplied by factor of safety.

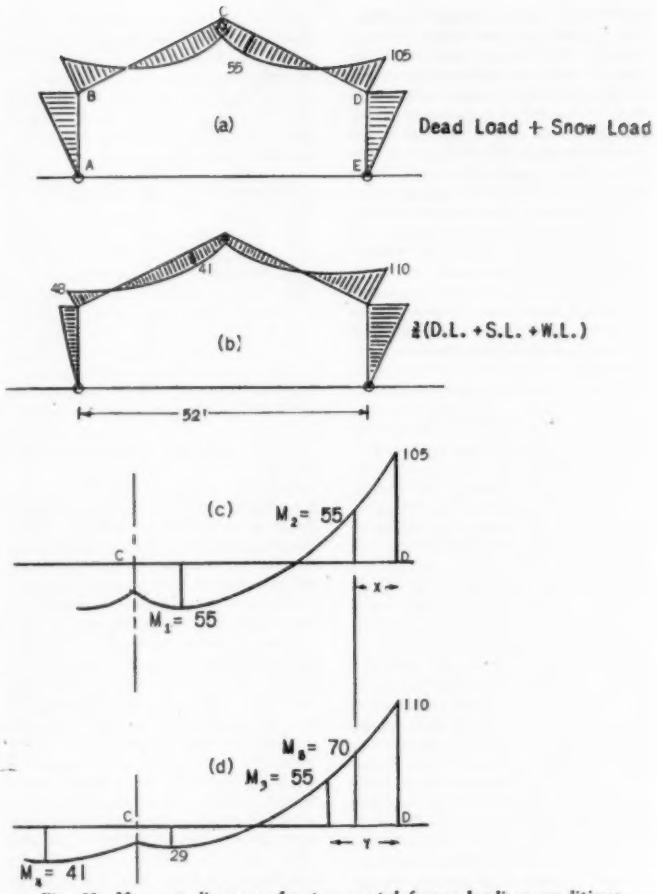


Fig. 83 Moment diagrams for two portal frame loading conditions

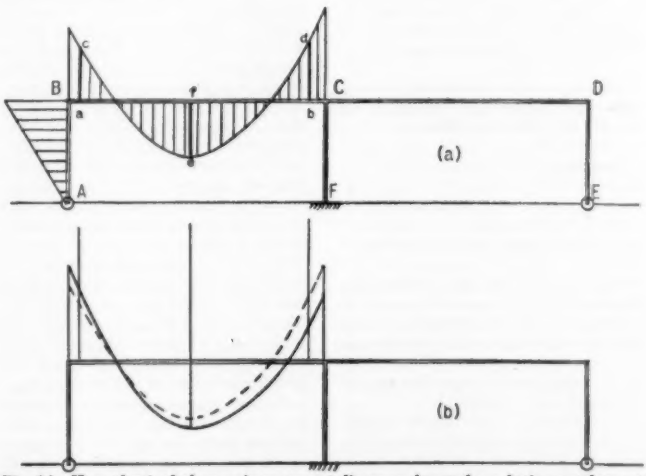


Fig. 84 Hypothetical change in moment diagram due to foundation settlement

If rotation capacity *were* required at a haunch, it seems that the most economical approach would involve making the haunch sufficiently strong so that yielding would first occur at the splice point where adequate plastic characteristics can be more easily assured.

Even if a haunched knee had *no* rotation capacity, the plastic behavior of the girder would still allow the frame designed for

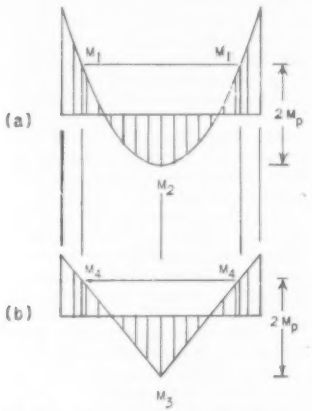


Fig. 85 Hypothetical moment diagrams for portal frame girders

the moment diagram shown in Fig. 85 (*a*) to carry the moments of Fig. 85 (*b*) (assuming that other possible limitations to plastic design were met). A plastic "hinge" would form in the girder at point *B*, after which the moments at the haunch splice (M_4) would increase to M_1. It would be required that $(M_3 + M_4) \leq (M_1 + M_2)$ and that the beam have adequate rotation capacity. Thus the extra material required by $M_3 > M_2$ might be saved.

4D. Summary of Plastic Behavior of Various Connection Types

(*1*) *Straight Knees* (*A, K, L, M, P*): The four straight knees *A*, *K*, *L* and *M* show an adequate reserve of strength beyond the elastic limit as shown by the comparison in Column 6 of Table 6. Although *A* and *K* are slightly deficient, they also develop the full plastic strength of the rolled section at the end of the knee (Column 10, Table 6).

Connections *L* and *M* have adequate rotation capacity due in part to improved lateral support and to the stiffening of the girder web in the case of Connection *M*. For some applications the indicated rotation capacity of Connections *A* and *K* might be insufficient.

Connection *P* develops neither the required strength, stiffness, nor rotation capacity.* This is due to the insufficient web thickness, although in the final stages of the test local buckling occurred together with the formation of a fracture. Both of these phenomena were probably factors leading to collapse. The test was stopped as soon as it had been established that the load had passed the maximum.

Although the fracture did not lead to serious trouble, the test of Connection *P* indicates a detail which should be avoided. Referring to Fig. 24 the design requires that the tension load in the column flange be transferred to the girder web. Since in the vicinity of the cut-away portion of the stiffener only the web itself is available to transmit a proportion of this flange load, a stress concentration exists and in this case it led to failure.† Therefore, use of the end plate detail as in Type 8B connections is to be preferred, accomplishing a more even transfer of load and in addition providing further economy.

(*2*) *Tapered Haunches:* Connections *D*, *E* and *F* have an adequate reserve of strength beyond the elastic limit (Column 6 of Table 6) and develop the full beam strength at the end of the rolled section (Column 11). The improved behavior of *F* over *D* (the latter possesses the greater stiffening) is attributed to more effective lateral support which also probably accounts for the improved rotation capacity of Connection *F*. If such connections were to be used in lieu of straight knees, then lateral support should be provided at the ends of the haunch if good rotation capacity is to be obtained rather than at the center of the compressive flange as was done in these tests.

Connections *C* and *N*, as tested under symmetrical loading, developed adequate plastic strength at one end of the haunch and not at the other. This is to be expected since one end is subjected to more severe bending moment than the other. If lateral support were provided at both ends of the haunch, improved behavior might be expected.

Connection *B* does not develop either the yield strength or the plastic strength of the beam at the end of the haunch. Neither does it develop the theoretical initial yield strength, and the reasons for this have been discussed earlier. Under the loading used, the flanges yield simultaneously throughout their length. Just as in the case of the control beam under uniform moment (Fig. 68) the connection collapses rapidly after such yielding. On the other hand, when the same member has a steep moment gradient as in the case of straight knees (*A, K, L, M*) it has no difficulty in carrying a moment that is even greater than the maximum control beam moment.

Probably the simplest means of improving the design of this connection is to increase the inner flange thickness. With this improvement it would be an effective connection for design use. It is not desirable to allow a connection with the proportion of Connection *B* to yield at the "full load."

(*3*) *Curved Knees:* Of all the knees tested, the curved connections show the greatest reserve strength beyond the elastic limit (Column 6, Table 6). The three connections, *G*, *H* and *I* develop the yield strength of the rolled section at the splice and also develop there the plastic hinge strength of the rolled section (Column 10, Table 6). The procedures recommended by the American Institute of Steel Construction[1] for the design of curved knees thus appear to have additional merit in the plastic range of stress. The rotation capacity improves with decrease in radius of curvature and increase of flange thickness. In fact, at a slight decrease in moment, Connection *I* satisfies the requirements mentioned earlier for rotation capacity of straight knees.

Prior to collapse, Connection *J* developed the yield strength of the rolled section but fell just short of developing its full strength. Thus a system of vertical stiffening appears to be somewhat less effective than increased flange thickness as specified by the AISC rules.[1] Compared with Connections *D*, *E* and *F*, both the strength and rotation capacity of Connection *J* are greater.

5. *Economy of Fabrication*

The bending moment diagram for a rigid frame usually falls off sharply from the center of the knee; the greater the length of haunch the greater the reduction. Thus the splice moment may be materially less than the haunch moment. As a consequence, the members joined by a haunched knee may be considerably lighter in weight than those required for a straight knee. Therefore, the greater the length of knee the greater the economy of rolled beams.

An increase in haunch length, however, is accompanied by an increase in cost of fabrication. Since the selection of the members of a frame is dependent on the moment diagrams, which vary for each span, height and loading condition, no general comparison can be made in this paper. Attention is called, however, to the relation between knee lengths and the relative time of cutting and welding as shown in Fig. 86. Except for Connections *N* and *J*, it is evident that fabrication time (exclusive of handling) increases in almost direct proportion to the total length of the connection. This length is measured along the neutral lines from the haunch point to the splice between the connection and the rolled shape.

Examining the straight knees as a group it is evident from Fig. 10 that Connection *A* is the most economical. However an additional factor to be considered is that special plant equipment might be required

* Depending on the proportions of a frame of which Connection *P* was a part and upon the deflection limitation, Connection *P* *might* be considered adequate if it were the first plastic hinge to form.

† The beam end was rough-finished, creating additional stress concentrations. However, the fracture did not occur until considerable local necking had taken place.

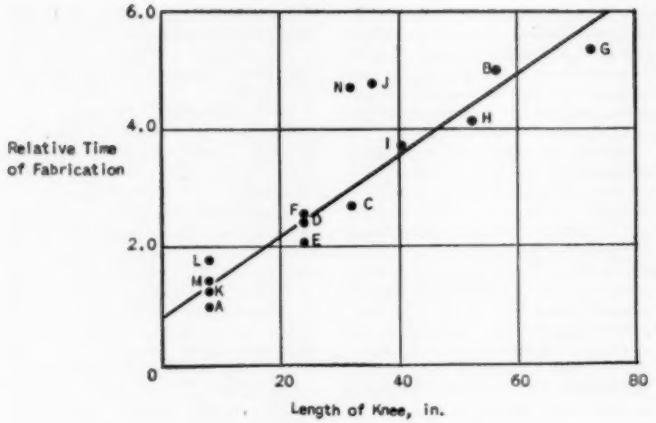


Fig. 86 Fabrication time as a function of haunch length

to make the 45-deg cut in large quantity production. Comparing Connections *K*, *L* and *M*, it is not evident from the tests that the increased cost of inserting vertical stiffeners is warranted unless the full plastic strength is to be developed and unless rotation capacity is specifically required. If this is the case then the use of vertical stiffeners is essential. The difference in cost between *L* and *M* is a measure of scatter since *L* should actually involve less welding time.

Examining Connections *D*, *E* and *F*, the use of half-depth stiffeners indicates an advantage over those of full depth. From a strength point of view there is little to separate the two designs so that some real economy might be realized by the use of half-depth stiffeners if large numbers of connections were involved. Providing *F* and *D* do not differ because of experimental scatter, Fig. 10, it is somewhat more expensive to bevel the "inner" stiffener, even though this eliminates a line of welding as compared with *D*. Undoubtedly, however, if the sniped plates were prepared in mass production, Connection *F* would be more economical since a special fitting operation is required for the vertical inner stiffener used in Connections *D* and *E*. Thus on a cost basis *E* is to be preferred over *F* and *F* over *D*.

A comparison may be made between Connections *B* and *H*. Both are about the same length (Fig. 86). *B* is about 25% more expensive, although this ratio would be reduced by the extra cost of material for *H* and the expense of rolling the curved inner flange. As is seen from Fig. 52 and the comparisons contained in earlier sections, the load carrying capacity of Connection *H* is considerably greater than that of *B*.

Further general or specific comparisons are difficult to make, since economy of rolled section, carrying capacity of haunch and expense of haunch fabrication must all be considered.

In arriving at an economical design from the over-all viewpoint of cost of members and of fabrication of the joint, one might suggest beginning with a trial selection of straight member using straight knees. To be compared with the above trial design is another in which the members are lighter in weight and not capable of carrying the knee moment. The points near the joint where strengthening must begin can be ascertained from the moment diagram, and thus the knee lengths are established and the joint designed. A comparison of costs may now be made which will include cost of members and cost of knee fabrication.

The introduction of curved knees and tapered haunches into a design also allows for a savings in fabricating costs due to the fact that a smaller rolled shape would be handled, joined and fabricated than would be required in case straight knees were used.

The disproportionate expense of Connection *J* (Fig. 86) does not appear to warrant this form of construction.

6. *Further Research*

In the interest of shortening this paper a separate note on proposed research has been prepared.[38] Generally a means of improving straight knee performance is warranted if it can be done at small extra expense. The influence of size and shape of cross section requires further examination. In the built-up connections a considerable amount of analytical work remains to be done and further tests are warranted at the present time.

II. SUMMARY AND CONCLUSIONS

The statements and conclusions which follow are based on the conections tested and the rolled shapes examined.

1. *Connection Design Details*

(*1*) *Type 2* (*Connection A*): (*a*) By taking into account both shear and moment the experimental elastic moment-rotation curve can be predicted by theory within about 10%.

(*b*) The Type 2 connection develops adequate strength but the Type 8B connection may be preferable because of equipment cost factors and because of improved performance in the plastic range.

(*c*) This connection takes slightly less time to fabricate than the Type 8B connections.

(*2*) *Type 2B* (*Connection B*): (*a*) This knee did not quite develop the moment at the haunch corresponding to the initial yield condition.

(*b*) When compared to the other built-up models, the less desirable behavior of this connection under the "worst loading condition" was due primarily to the fact that the whole length of the inner compression flange yielded simultaneously. Residual stress was a factor since it caused this yielding to occur at a lower load than predicted, further aggravating the tendency toward lateral buckling.

(*c*) On the basis of this one test, the initial yield load marked the limit of carrying capacity. Improved performance is to be expected if a thicker compression flange is used in the design.

(*d*) The average unit rotation, ϕ_h, for the knee may be determined approximately by the expression

$$\phi_h = \frac{2d^2}{d_h^2} \frac{M_h}{EI}$$

where

d = rolled section depth.
d_h = haunch depth.
M_h = haunch moment.

(*3*) *Type 4* (*Connections D, E, F*): (*a*) The best design of these three knees is Connection *E*. Further improvement could be realized by using a sniped full-depth stiffener as an extension to the inner column flange. Additional economy could be realized by replacing the external pair of stiffeners with an end plate as in the Type 14 connection, Fig. 4.

(*b*) The rolled section strength is fully developed at the splice, and there is adequate reserve of strength above the yield point. This connection was not tested under a "worst loading condition."

(*c*) Lateral support should be provided at the splices if good rotation capacity is to be obtained in the plastic range.

(*4*) *Type 5A* (*Connections G, H, I, J*): (*a*) The curved knees designed according to the AISC recommendations[1] (*G*, *H* and *I*) developed both the initial yield strength and the full plastic strength at the end of the rolled section. The reserve strength above the predicted yield load is more than adequate.

(*b*) The average rotation per unit length is about one-third that of the rolled beam, and for the same thickness of curved inner flange may be determined within 10% by the expression given in paragraph (2) (*d*) above.

(*c*) Stiffening of inner flanges as in Connection *J* in lieu of increased flange thickness is not to be recommended on the basis of these tests. Although such stiffening improved rotation capacity, it is comparatively uneconomical.

(*5*) *Type* (?) (*Connection P*): (*a*) The elastic stress distribution in adjacent beam sections agrees with ordinary beam theory except in the local region within a distance from the knee of half the depth of the section.

(*b*) Within the knee, flange stresses decrease linearly from the critical section at the splice to the external end of the flange. Shear stresses in the web are larger than those at the critical section for bending.

(*c*) Yielding due to shear force in the knee web occurred at about 50% of the moment corresponding to the flexural yield point. Agreement with theory was within a few percent. The subsequent additional rotation could not be tolerated if continuous frame analysis were to be applied. Diagonal stiffeners are recommended in order that such premature yielding may be avoided. The expression $w \geq 2S/d^2$ (in which S = section modulus and d = girder depth) gives the required web thickness to prevent premature web yielding.* Using this relationship, all WF and I shapes have been examined; it has been found that *no* WF shape has adequate web thickness, and in the American Standard I series only the *heaviest* shapes have adequate web thickness.

(*d*) The connection type had somewhat inadequate strength characteristics since it fell short of developing the computed yield strength of the weaker of the two members joined.

(*e*) In the elastic range this connection was more flexible than predicted by the theory developed. Further, the flexibility of the connection was twice as great as that implied in ordinary deflection calculations based on complete rigidity at the connections. If the web thickness, w, were such that $w \geq 2.6S/d^2$ then, adequate elastic stiffness is assured.

(*f*) A lapped flange plate is preferable to web stiffeners for transmitting the tension load in the exterior column flange to the web of the girder. Such a scheme is used in Type 8 and 14 connections, Fig. 4.

(*6*) *Type 8B* (*K, L, M*): (*a*) The diagonal stiffening was adequate to prevent serious yielding due to shear force.

(*b*) Excellent agreement between theoretical and experimental moment-rotation curves was obtained in the elastic range. This rotation is somewhat greater (14%) than the equivalent length stiffness implied in the ordinary computation of deflections in continuous structures.

(*c*) These connections yield at a considerably lower load than predicted, probably due to residual stress and stress concentrations. However, the ultimate strengths developed are very close to the plastic hinge moment.

(*d*) The half-depth stiffener as used in *L* is preferred, decreasing the small influence of residual stress. It is doubtful that *K* has adequate rotation capacity, whereas Connections *L* and *M* are excellent in this respect.

(*7*) *Type 15* (*C*): (*a*) This knee would normally be designed for a nonuniform moment gradient but was tested with an equal moment gradient on each leg. This amounts to examining the connection under a second loading condition. Although it develops the plastic hinge strength at one end, it does not reach the yield or full plastic strength at the other splice. Little improvement in this characteristic could be obtained with a more rigid system of lateral support, since the strength at Section *b-b* (see sketch, Table 1) was somewhat greater than obtained in Connection *L* with its efficient lateral support system.

(*b*) The elastic average unit rotation is given approximately by the expression of paragraph (2) (*d*) above.

(*8*) *Type 16* (*N*): The behavior of Connection *N* is similar to that of Connection *C* described above.

2. *Structural Behavior*

(*1*) *Yield Strength:* (*a*) The following criteria were selected for evaluating the behavior of the various connections:

1. "Yield Line Moment" (M_1)—the moment at which the first yield line is observed.
2. "Visual Yield Moment" (M_2)—the moment at which the plotted curve becomes nonlinear as visually observed.
3. "General Yield Moment" (M_3)—a moment determined by graphical construction to indicate significant inelastic deformation.
4. "Deformation Increment"—the increment of deflection beyond the predicted value at the computed elastic limit moment.

(*b*) In general, the first yielding occurred at about 50% of the computed initial yield moment. This yielding was due to residual stress and stress concentrations. At about 70% of the computed moment the departure of the experimental curves from linearity could be detected visually. The "General Yield Moment" was, on the average, greater than the predicted initial yield moment, indicating a good reserve of strength beyond the elastic limit. The deformation increment at the theoretical elastic limit ranged from 5 to 24%.

(*c*) Nearly all of the connections developed "General Yield" strengths greater than a moment corresponding to initial yield at the end of the connection.

(*2*) *Ultimate Strength:* (*a*) Only the Type 7 connection was unable to carry a haunch moment equivalent to the plastic hinge value of the rolled section. All of the haunched connections developed strengths greater than any of the straight connections.

(*b*) All of the connections except *B* and *P* developed a moment-carrying capacity considerably greater than the calculated initial yield moment. Simple expedients will improve the performance of the two connections mentioned.

(*c*) A few of the haunched connections did not develop the full rolled beam strength at the haunch ends. An increase of inner flange thickness would improve the behavior of such connections.

(*3*) *Stiffness:* (*a*) Expressions for elastic stiffness of straight knees have been developed which give fair agreement with Type 7 connections and good agreement with Types 2 and 8B (diagonally stiffened). The development also includes (in Part II) expressions for the rotational stiffness of straight knees of the three types with equal and with unequal depths of rolled shapes meeting at the connection.

(*4*) *Plastic Instability:* (*a*) Plastic instability was involved in the collapse of every connection and in most cases brought about final collapse. In some instances collapse followed quickly after first local buckling; in others it was postponed. For plastic design, a specification must be developed for proper geometric proportions of rolled shapes to prevent premature plastic local buckling.

(*b*) Deformations increased rapidly once local buckling occurred. In the case of straight knees in which it was possible to provide lateral support at the most critical point, collapse did not occur when the first local buckling became evident, since the buckling was symmetric on both sides of the web. However, when a half-wave was formed on only one side of the web, collapse followed rapidly.

(*c*) The seriousness of local buckling was markedly reduced whenever lateral support could be placed at each point of expected yielding.

(*5*) *Lateral Support:* (*a*) It is desirable to place lateral support at points of expected maximum stress, thus reducing the seriousness of local buckling.

(*b*) A rather small force is required initially to prevent sidewise deformation, the force increasing rapidly after local buckling.

(*c*) Compression flange lateral support should be provided at the center of built-up haunches and at the splice points between haunches and beam.

(*d*) Except for variations in cost, so far as plastic strength is concerned, effective

* Further tests designed specifically to check this expression are advisable.

lateral support is more important than the variations in fabrication details.

(*f*) ***Rotation Capacity*** (as influencing plastic design): (*a*) A straight connection must have "rotation capacity," the ability to rotate through a considerable unit angle change after the plastic hinge moment has been reached if the frame is to be treated in accordance with plastic design technique. The amount depends on the proportions and loading of the frame. By way of illustration, a rotation capacity of about eight times the rotation at initial yield is required in the case of a third-point-loaded beam attached to very stiff columns. With adequate lateral support the Type 8B connections are satisfactory in this respect while the behavior of Type 2 is not.

(*b*) Most of the built-up knees collapse rapidly after first local buckling. However, rotation capacity usually is not required of haunched connections. Such connections are generally associated only with elastic design principles.

(*c*) When rotation capacity *is* desired, it could be obtained in some haunched connections by increasing the strength of the knee slightly beyond that required elastically so that yield would occur at the point where the haunch and girder are joined.

(*d*) Rotation capacity implies the ability of the knee to deform without exhibiting marked local or lateral buckling. Thick flanges and effective lateral support are most helpful in this respect.

(7) ***Cost of Fabrication:*** (*a*) There is an almost linear relation between the time required for cutting and welding and the total length of haunch as measured along the neutral lines. Connections *N* and *J* are exceptions.

(*b*) Sniped, half-depth stiffeners are satisfactory and are less expensive than either the full-depth sniped stiffeners or the complete stiffeners welded to the web and to both top and bottom flange.

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NOMENCLATURE

A = area of section.
A_w = area of web.
A_s' = equivalent stiffener area.
b = flange width.
b_s = total width of stiffener.
d = depth of section.
d_h = "haunch depth," the minimum distance from the external corner of the haunch to the inner flange.
w = web thickness.
t = flange thickness.
t_s = stiffener thickness.
I = moment of inertia of section.
I_f = moment of inertia of the flanges only.
S = section modulus of beam.
L = distance between the point of inflection and the haunch point in an actual frame.
ΔL = equivalent length of a connection.
M_h = "haunch" moment.
$M_{h(\tau)}$ = "haunch" moment at which yield occurs due to shear force.
$M_{h(\sigma)}$ = "haunch" moment at which yield occurs due to flexure.
M_r = moment in a connection at junction of rolled beam and connection.
M_p = "hinge" value; full plastic moment; the ultimate moment that can be reached according to the simple plastic theory.
M_a = "hinge" value at haunch.
$M_{(i)}$ = initial yield moment.
$M_{(y)}$ = moment at which yield point stress is reached at the end of the rolled section.
$M_{(1)}$ = observed yield line moment.
$M_{(2)}$ = observed visual yield moment.
$M_{(3)}$ = observed general yield moment.
$M_{(4)}$ = observed maximum moment.
σ_y = average lower yield point stress.
E = Young's modulus of elasticity.
G = shearing modulus of elasticity.
ϕ = average unit rotation.
ϕ_A = rotation measured over equivalent length of connection.
γ = rotation in connection due to shear.
β = rotation in connection due to bending.

TERMINOLOGY

General yield moment: The moment at which the deformations due to this moment begin to affect the structural behavior elsewhere in a structure. The value is determined graphically as shown in Fig. 76.

Haunch depth: The minimum distance from the external corner to the inner flange.

Haunch moment: The moment at the haunch point.

Haunch point: The intersection of the extended neutral lines of girder and column.

Initial yield moment: A computed moment at which the nominal maximum stress reaches the yield point, excluding the influence of stress concentrations and residual stresses.

Lüder's line: Wedge or plane of yielding which forms in mild steel in the vicinity of the yield point.

Plastic hinge moment: The ultimate moment that can be reached at a section according to the simple plastic theory.

Rolled section moment: The moment at the junction of the rolled beam and the knee.

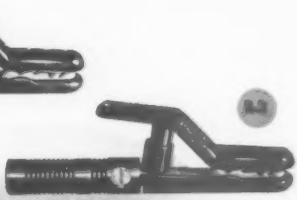
Rotation capacity: The ability of a structural member to rotate under near-constant moment.

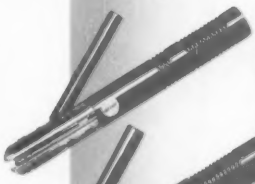
Visual yield moment: The moment at which the plotted curve becomes nonlinear, as observed visually (see Fig. 76).

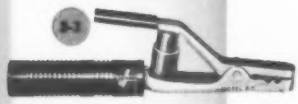
Yield line: Flaking of mill scale following the formation of Lüder's line as revealed by whitewash.

Yield line moment: The moment at which the first yield line is observed (see Fig. 76).

Yield strength: The load or moment at which a significant amount of yielding occurs as indicated by available criteria.







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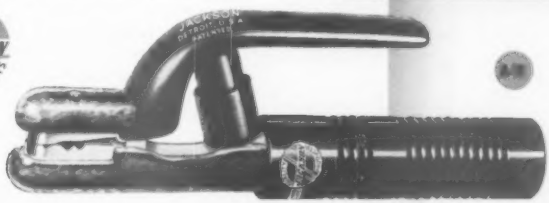

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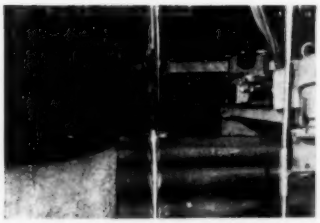

GOING... This section was first heated to 600°F. After five minutes of torch preheating, the 6850 was put into action at a forward speed of approximately 1.5 inches per minute. Halfway through this huge section, the cut is clean and symmetrical . . . yet the operation is virtually unattended!

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